DR. 387

MASTER

SAN-679-8

**DESIGN STUDY
ELECTRICAL COMPONENT TECHNOLOGY FOR 0.25
TO 10.0 MEGAWATT SPACE POWER SYSTEMS**

Final Report

Edited by
A. E. King

February 1, 1968

Aerospace Electrical Division
Westinghouse Electric Corporation
Lima, Ohio

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

LEGAL NOTICE

This report was prepared as an account of Government sponsored work. Neither the United States, nor the Commission, nor any person acting on behalf of the Commission:

A. Makes any warranty or representation, expressed or implied, with respect to the accuracy, completeness, or usefulness of the information contained in this report, or that the use of any information, apparatus, method, or process disclosed in this report may not infringe privately owned rights; or

B. Assumes any liabilities with respect to the use of, or for damages resulting from the use of any information, apparatus, method, or process disclosed in this report.

As used in the above, "person acting on behalf of the Commission" includes any employee or contractor of the Commission, or employee of such contractor, to the extent that such employee or contractor of the Commission, or employee of such contractor prepares, disseminates, or provides access to, any information pursuant to his employment or contract with the Commission, or his employment with such contractor.

This report has been reproduced directly from the best available copy.

Printed in USA. Price \$3.00 Available from the Clearinghouse for Federal Scientific and Technical Information, National Bureau of Standards, U. S. Department of Commerce, Springfield, Virginia 22151.

LEGAL NOTICE

This report was prepared as an account of Government sponsored work. Neither the United States, nor the Commission, nor any person acting on behalf of the Commission:

A. Makes any warranty or representation, expressed or implied, with respect to the accuracy, completeness, or usefulness of the information contained in this report, or that the use of any information, apparatus, method, or process disclosed in this report may not infringe privately owned rights; or

B. Assumes any liabilities with respect to the use of, or for damages resulting from the use of any information, apparatus, method, or process disclosed in this report.

As used in the above, "person acting on behalf of the Commission" includes any employee or contractor of the Commission, or employee of such contractor, to the extent that such employee or contractor of the Commission, or employee of such contractor prepares, disseminates, or provides access to, any information pursuant to his employment or contract with the Commission, or his employment with such contractor.

SAN-679-8

PROPULSION SYSTEMS AND ENERGY
CONVERSION (TID-4500)

Aerospace Electrical Division
Westinghouse Electric Corporation

FINAL REPORT

DESIGN STUDY
ELECTRICAL COMPONENT TECHNOLOGY
FOR 0.25 TO 10.0 MEGAWATT
SPACE POWER SYSTEMS

Prepared Under
Contract AT(04-3)-679
for the
San Francisco Operations Office
U.S. Atomic Energy Commission

Lima, Ohio

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

fly

FINAL REPORT

DESIGN STUDY
ELECTRICAL COMPONENT TECHNOLOGY
FOR 0.25 TO 10.0 MEGAWATT
SPACE POWER SYSTEMS

February 1, 1968

Edited by:

A. E. King

A. E. King, Project Engineer
Electromechanical Systems Development

Approved by:

F. C. Stump

F. C. Stump, Program Manager
Electromechanical Systems Development

N. W. Bucci R.

N. W. Bucci, Engineering Manager
Advanced Systems Department
Aerospace Electrical Division
Westinghouse Electric Corporation

Prepared Under
Contract AT(04-3)-679
for the
San Francisco Operations Office
U.S. Atomic Energy Commission

FOREWARD

This Final Report summarizes work under contract to the United States Atomic Energy Commission from September 1, 1966 to February 29, 1968, on a Design Study of Electrical Component Technology for 0.25 to 10 Megawatt Space Power Systems.

The work is directed toward defining representative Rankine nuclear space power electrical systems, the electrical components therein and their development problems. The work was conducted by the Westinghouse Electric Corporation, Aerospace Electrical Division under contract AT(04-3)-679 administered by the USAEC San Francisco Operations Office. Technical liaison is maintained with the USAEC Division of Space Nuclear Systems, Washington, D. C., Mr. G. S. Leighton, Liaison Officer, and with the Reactor Division of the San Francisco Operations Office, Mr. F.W. Hunton, Liaison Officer.

Members of the Westinghouse Systems Research and Development Department assisting in the preparation of this report include A. E. King, J. B. Fanger, T. C. Allen, C. L. Doughman, J. W. Gyurek, J. G. Hoop, R. D. Jessee, C. C. Kouba, J. L. McCabria, and E. F. Swiderski.

Contributing Authors from other Westinghouse locations include D. V. Foster of the Astronuclear Laboratories.

This report is assigned contractor designation Report Number WAED 68.02E.

ABSTRACT

Analytical conceptual designs are presented for purposes of illustrating recent technology for electrical power system components in advanced high-temperature, potassium, Rankine nuclear space power systems in the 0.25 to 10 megawatt range. Data presented is based on extensive backgrounds in the development of high temperature electrical materials, high temperature ac generators and motors, and power conditioning equipment for space application and is intended for application in future nuclear-electric powerplant system or mission studies. Since requirements of the electrical power systems are related to the missions, the design studies were carried out using three representative missions - interplanetary, orbital and planetary base. These were analyzed for the purpose of defining the ranges and types of electrical power needs and corresponding components.

CONTENTS

1.	SUMMARY.....	1
1.1	Scope of Study.....	2
1.2	Contract Reports.....	5
2.	SYSTEM DEFINITIONS.....	7
2.1	Power Uses and Missions Definitions.....	7
2.2	Nuclear Mechanical Systems Definitions.....	27
2.3	System Definition for Conceptual Designs.....	67
3.	CONCEPTUAL DESIGNS.....	81
3.1	Generators Conceptual Designs.....	81
3.2	AC Motors Conceptual Designs.....	161
3.3	Power Conditioning Components.....	184
4.	CONCLUSIONS.....	209
5.	REFERENCES.....	211

APPENDICES

A	Generator and Motor Stator Bore-Seals and Rotor Cans.....	217
B	Notes on Procedure for Calculation of Rotor Stresses.....	225

FIGURES

2.2-1	Turbine Shaft Power Specific Weight.....	24
2.2-2	Flow Diagram for Rankine Cycle with Liquid Coolant Radiator.....	32
2.3-3	Manned Vehicle Shield Weights.....	35
2.2-4	Unmanned Vehicle Shield Weights.....	37
2.2-5	Estimated Power Distribution for 0.35 MWe System (Unmanned).....	48
2.2-6	Estimated Power Distribution for 1.0 MWe System (Unmanned).....	49
2.2-7	Estimated Power Distribution for 5.0 MWe System (unmanned).....	50
2.2-8	Estimated Power Distribution for 10.0 MWe System (Unmanned).....	51
2.2-9	Rotor Kinetic Energy Versus Generation Electrical Rating.....	59
2.2-10	Time Required to Accelerate Rotor to 5 Percent Overspeed Versus Generator Electrical Rating.	59
2.2-11	Off-Design, Coolant Flow Changes.....	66
2.3-1	Preliminary Concept No. 1 for Unmanned Scien- tific Probe.....	68
2.3-2	Preliminary Concept No. 1 for Manned Inter- planetary Mission.....	69
2.3-3	Electric Engine Module (1 of n).....	70
2.3-4	Preliminary Concept No. 1 Synchronous Orbit Space Station.....	71
2.3-5	Concept for Lunar Base.....	72
2.3-6	Multiple Power Source Concept of Achieving a Specific Rating.....	74
2.3-7	Single Line Diagram of Isolated-Load Bus System.....	76
2.3-8	Generator Excitation Control Circuits.....	77
3.1-1	Parametric Weight and Efficiency Summary, Alternator.....	84
3.1-2	Specific Weight Parametric Summary, Alternator.	85
3.1-3	Parametric Speed Summary, Alternator.....	86
3.1-4(a)	400 kVA Alternator.....	91
3.1-4(b)	1200 kVA Alternator.....	93
3.1-4(c)	6000 kVA Alternator.....	95
3.1-4(d)	12,000 kVA Alternator.....	97
3.1-5	Field Coil Design Comparison, 0.4 MVA Alterna- tor.....	102
3.1-6	Field Coil Design Comparison, 1.2 MVA Alterna- tor.....	103

FIGURES (Continued)

3.1-7	Field Coil Design Comparison, 6.0 MVA Alternator.....	104
3.1-8	Field Coil Design Comparison, 12.0 MVA Alternator.....	105
3.1-9	Field Coil Temperatures, 0.4 MVA Alternator....	106
3.1-10	Field Coil Temperatures, 1.2 MVA Alternator....	107
3.1-11	Field Coil Temperatures, 6.0 MVA Alternator....	108
3.1-12	Field Coil Temperatures, 12.0 MVA Alternator...	109
3.1-13	Effect of Slot Conductance on Average Winding Temperature.....	114
3.1-14	Comparison of Slot Conductance.....	115
3.1-15	Slot Configuration.....	116
3.1-16	Conceptual Design Stator Wire Temperatures.....	119
3.1-17	Armature Operating Temperatures, 0.4MVA Conceptual Designs.....	120
3.1-18	Armature Operating Temperatures, 1.2 MVA Conceptual Designs.....	120
3.1-19	Armature Operating Temperatures, 6.0 MVA Conceptual Designs.....	121
3.1-20	Armature Operating Temperatures, 12.0 MVA Conceptual Designs.....	121
3.1-21	Material Strengths of H-11 Steel at Rockwell C45.....	126
3.1-22	Allowable Material Strengths of H-11 Steel at Rockwell C45.....	127
3.1-23(a)		129
thru		thru
3.1-23(d)	Rotor Stress Curves.....	132
3.1-24(a)		133
thru		thru
3.1-24(k)	Rotor Stress Curves.....	143
3.1-25(a)		144
thru		thru
3.1-25(d)	Rotor Stress Curves.....	157
3.1-26(a)		148
thru		thru
3.1-26(d)	Rotor Stress Curves.....	151
3.1-27	Generator Radial Gap.....	160
3.1-28	Alternator Weight Comparison, 800°F Conceptual Designs.....	164
3.2-1	AC Induction Motor, 5 HP.....	167
3.2-2	AC Induction Motor, 30 HP.....	169
3.2-3	AC Induction Motor, 60 HP.....	171
3.2-4	AC Induction Motor, 250 HP.....	173
3.2-5	Stator Slot Configuration.....	178

FIGURES (Concluded)

3.2-6	Rotor Slot Configuration.....	179
3.2-7	Stator Heat Transfer Paths.....	181
3.2-8	Rotor Heat Transfer Paths.....	182
3.3-1	Semiconductor Cycloconverter, 5 kVA.....	191
3.3-2	Semiconductor Cycloconverter, 25, 50, and 100 kVA.....	193
3.3-3	Thyristor Configuration for Frequency Changer Conceptual Design.....	195
3.3-4	Rectifier Layout, 25 and 75 kW.....	199
3.3-5	Rectifier Layout, 250 and 500 kW.....	201
3.3-6	Three-Phase Transformer Conceptual Design Data	204
3.3-7	Transformer Conceptual Design Layouts.....	205
3.3-8	Three-Phase Transformer Conceptual Design Data	207

TABLES

2.2-1	Powerplant component Weights.....	28
2.2-2	Average Ratio of Total System Specific Weights at 40,000 and 10,000 hours.....	29
2.2-3	NASw-673 Reactor Designs.....	33
2.2-4	Reactor Core Dimensions.....	34
2.2-5	Reference Turbine Design.....	38
2.2-6	Turbine Weights.....	38
2.2-7	Primary Radiator Area & Weight.....	41
2.2-8	Primary Radiator Loop Pump Power.....	42
2.2-9	Primary Loop Expansion Compensator Weights.....	42
2.2-10	Radiator Loop Expansion Compensator Weights....	43
2.2-11	Reactor Coolant Pump Powers.....	43
2.2-12	Boiler Feed Pump Powers.....	43
2.2-13	Summary of Estimated Electrical System Losses (kW).....	46
2.2-14	Summary of Estimated Electrical System Coolant Flows (lb/sec).....	47
2.2-15	Auxiliary and Secondary Radiator Areas (ft ²)...	52
2.2-16	Auxiliary and Secondary Coolant Pump Powers (hp).....	53
3.1-1	Summary of Generator Parametric Study, Selec- ted Speeds, Frequency and Voltages per Reference 6.....	82
3.1-2	Conceptual Generator Parameters.....	87
3.1-3	Field Coil Design Parameters Per Basic Generator Designs of Table 3.1-2.....	99
3.1-4	Recommended Field Coil Configurations For Minimum Temperature/Loss Designs.....	101
3.1-5	Current Density Study, Average Winding Tempera- ture.....	113
3.1-6	Bore Seal Thickness Summary.....	125
3.1-7	Comparison of Rotor Stresses.....	152
3.1-8	Comparison of Rotor Resistivity.....	153
3.1-9	Comparison of Rotor Effective Permeability (c.g.s.).....	153
3.1-10	Comparison of Pole Face Losses and Effects.....	155
3.1-11	Pole Tip Temperatures, Laminated Poles.....	156
3.1-12	Parametric Study Designs.....	158
3.1-13	Percent of Pole Face Loss.....	160
3.1-14	Conceptual Designs.....	162
3.1-15	Weight Breakdown, Conceptual Design Alterna- tors.....	163
3.1-16	Loss Breakdown, Conceptual Design Alternators..	163

TABLES (Concluded)

3.2-1	Identification of Motor Conceptual Designs.....	165
3.2-2	Conceptual Motor Design Component Material.....	185
3.2-3	Conceptual Motor Design Performance.....	186
3.2-4	Conceptual Motor Design, Critical Temperatures and Coolant Requirements.....	187
3.2-5	Conceptual Motor Design Weight Breakdown.....	188
3.3-1	Cycloconverter Conceptual Design Summary, 200 Volt, 400 Hz Output, Power Factor 0.7.....	190
3.3-2	Rectifier Conceptual Design Summary.....	197
3.3-3	Three Phase Transformer Conceptual Design Summary (800°F Average Coolant Temperature)...	203

1. SUMMARY

The Aerospace Electrical Division of the Westinghouse Electric Corporation was under contract to the United States Atomic Energy Commission to define conceptual designs of complete electrical power systems and the electrical components therein suitable for Rankine nuclear space power systems in the 0.25 to 10.0 megawatt range. Three representative missions-interplanetary, planetary, and orbital - were specified in order that the conceptual definitions consider dependent design parameters typical of actual flight conditions. Work was carried on in design studies and in associated analytical verification experiments.

The design studies were carried out in two successive stages: (1) analytical screening studies to establish parametric data, optimum designs and design limitations representative of four system power levels, and (2) detailed studies of selected configurations to illustrate conceptual designs at selected power levels and the development problems associated with them. The electrical system components studied were ac generators, ac motors, frequency changers, power conditioning equipment, power transmission and control equipment, and electrical system control and protection equipment. The results of the studies should be applicable to: (1) defining the component development effort required to realize mission capabilities and (2) aiding the electrical system designer and nuclear system planner in specifying the optimum electrical configuration for specific missions.

Three analytical verification experiments were carried out to evaluate the adequacy of certain analytical techniques for an ac generator design. The experiments were specifically directed toward evaluating magnetic unbalance forces, magnetic flux distributions, generator losses at no-load conditions, and rotor coolant flow concepts.

The scope of the Design Study is summarized in the following paragraphs in order for the reader to better understand the extent of the work carried out and the relationships of the various tasks. There were three major tasks which contained a total of twelve separate but interrelated sub-tasks.

Summary of Design Study Tasks

Parametric Screening Studies

- Power Uses and Missions Definition
- Nuclear-Mechanical System Definition
- Generator Parametric Study
- AC Motor Parametric Study
- Frequency Changer Parametric Study
- Power Conditioning Parametric Study
- Power Transmission and Distribution Study

Conceptual Design Analyses
Electrical System Definition
Conceptual Design Analysis and Layout

Experimental Verification (generator only)
Experimental Evaluation of Magnetic Unbalance
Forces and Flux Distribution
Experimental Evaluation of No-Load Losses
Experiments Associated with Rotor Cooling
Concepts

1.1 SCOPE OF STUDY

1.1.1 Parametric Screening Studies

A. Power Uses and Missions. The mission requirements and environmental conditions that affect electrical component designs were included in the selection of the design conditions for three classes of missions:

- 1) interplanetary mission (electrical propulsion)
- 2) manned earth orbital, and
- 3) lunar or planetary base.

Basic specifications for each class of mission were provided by the Contracting Office from which Westinghouse determined the relative magnitudes of ac and dc power, voltage levels, bus configurations, etc. The results of this study are reported herein.

B. Nuclear-Mechanical System Definition. The requirements and limitations imposed on the electrical components by the prime mover system and associated equipment were included in the selection of the design conditions. Particular emphasis was placed on determining the electrical component requirements relative to prime mover system considerations. The results of this study are reported herein.

C. Generator Parametric Study. The five major system parameters evaluated relative to generator weight, efficiency, versatility and technology available were generator rating, speed, frequency, output voltage, and coolant temperature. The optimum (lightest, most efficient, and practical) combinations of frequency, speed and voltage were determined for four MVA ratings. Rather than arbitrarily examining all possible combinations, an iterative (recycling) process was used whereby the specific parameters were determined in as few combinations as possible. Two designs which were representative of the best designs above and which had hot-spot temperatures in excess of 1100°F were analyzed using advanced (1500°F) materials to evaluate possible weight and efficiency benefits. One design which was representative of the best designs previously mentioned was analyzed using 1400°F coolant to evaluate possible weight and

efficiency benefits. The results of this study are reported in detail in reference 6.

D. AC Motor Parametric Study. Parametric studies were conducted for three-phase induction motor electromagnetic components over a specified range of approximately 5 to 250 horsepower. These motors are expected to be used primarily for driving liquid metal circulating pumps. Secondary use considerations included fulfilling the Power Uses and Mission requirements. Motor parameters of particular interest included weight, efficiency and power factor as affected by operating voltage, frequency, speed, and cooling temperature. The selected operating frequencies were those obtainable from a static frequency changer as evaluated in the sub-task below and those coincident with the generator output frequencies. The resulting parametric data is in a form that it can be combined with centrifugal-pump data in order to assess the relative applicability and technological problems of these pump packages when compared to static electromagnetic pumps. The results of this study are reported in reference 5.

E. Frequency Changer Parametric Study. Parametric studies were conducted on three-phase frequency changers to operate primarily with the ac induction motors and electromagnetic pumps. Secondary consideration was given to configurations capable of supplying the requirements defined for the Power Uses and Missions. Frequency changer characteristic data included weight, efficiency, and maximum cooling temperature limitations based on existing state-of-the-art semiconductors and other static components. Design analyses were performed on operating multiple low-power frequency changers in parallel to obtain higher power levels. Three frequency changer designs were evaluated using high-temperature gas tubes such that direct comparisons could be made between low-temperature and high-temperature frequency changers. The results of this study are reported in detail in reference 4.

F. Power Conditioning Parametric Study. Parametric studies were conducted on power conditioning components for use in power systems rated in the range of 0.25 to 10 megawatts with primary emphasis placed on single-phase and three-phase transformers as well as three-phase rectifiers. Ratings up to several megawatts were examined for three-phase transformers and up to one megawatt for single-phase transformers. Three-phase rectifier systems were evaluated for dc voltage levels up to approximately 30,000 volts in accordance with the Power Uses and Mission requirements. Parametric data included weight, efficiencies, and cooling temperature limitations for various power and voltage levels and for both semiconductor and gas-tube rectifiers.

A compilation and evaluation of existing data on radiation effects to power conditioning components was performed for the purposes of summarizing the applicability of this data to megawatt power conditioning equipment. The results of this study are reported in detail in reference 4.

G. Power Transmission and Distribution Study. Studies were performed on electric power transmission systems to define parametric data on weights, power losses, impedances, operating temperatures and cooling requirements for both dc and ac power up to 10 megawatts. Voltages up to 30,000 volts for dc systems and up to 20,000 volts for ac systems were considered. The results of this study are reported in detail in reference 4.

1.1.2 Conceptual Design Analyses

A. Electrical System Definition. Electrical system configurations were analyzed for four power levels in the range of 0.25 to 10 megawatts. Relative magnitude of ac and dc power, voltage, bus configuration, etc., were selected to provide power as required for the Power Uses and Missions. Consideration was also given to parallel operation of ac busses, dc busses, load division, unbalanced loading, electrical faults, etc., in order to provide guidance for electrical system component technology developments.

Concepts were defined for controlling real and reactive load division in parallel systems, for sensing and isolating electrical faults, for transferring essential mission loads to unfaulted electrical busses, for supplying sudden increases or decreases in electrical loads and for supplying high quality electric power at closely-regulated voltages and frequencies.

Design analyses were performed on possible circuit configurations for supplying inductor generator field power of approximately 3 to 100 kilowatts. Special attention was given to selecting generator excitation control systems that are capable of functioning when multiple generators are connected in parallel. The results of this study are reported herein.

B. Conceptual Design Analyses. By screening the component configurations of the above parametric studies, optimum configurations were selected for generators, ac motors, frequency changers, and power conditioning equipment. The configurations were studied and further evaluated such that conceptual layout designs of the configurations could be made of sufficient dimensional detail for a weight-breakdown analysis. The designs of the components in the layouts were optimized relative to weight, losses, minimum fabrication complexities and state-of-development of the hardware concepts. The results of this study are reported herein.

1.1.3 Experimental Verification

A. Experimental Evaluation of Magnetic Unbalance Forces and Flux Distribution. A static magnetic mockup was prepared using available generator components from a previous 467 kVA experimental generator development program.* The mockup was fitted

*Task 2.4.4, Full Scale Generator, SNAP-50/SPUR Contract AF33(615)-2326

with search coils to measure magnetic flux levels at a minimum of eighteen different locations within and external to the magnetic circuit. The magnetic unbalance forces and torques created by an eccentric and misaligned rotor and stator were also measured. The measured forces, torque and flux distribution, both useful and leakage, were compared with design predictions to verify present analytical calculation techniques and indicate where improvements might be appropriate. The results of this study are reported in reference 7.

B. Experimental Evaluation of No-Load Losses. The static mockup of the above experiments was modified to permit dynamic operation using water cooling and oil-lubricated bearings. The experiments were conducted to measure the no-load losses (pole face, magnetic and field coil) and corresponding temperature distributions at constant speed. The measured data was compared with the analytically calculated predictions and with the available materials data to verify its adequacy and indicate appropriate improvements. The results of this study are reported in reference 7.

C. Experiments Associated with Rotor Cooling Concepts. The fluid dynamic test rig from the same Air Force Contract* was modified to include a rotor with radial flow pole passages for the rotor coolant. Experiments were run with "easy-fluids" that simulated a potassium rotor coolant to determine rotor flows, pressures, and temperatures as a function of speed.

1.2 CONTRACT REPORTS

Other reports published on this contract are listed below in their order of occurrence in section 5 "References" of this report.

- a) Quarterly Technical Progress Report No. 1, SAN 679-1, September 1 to November 30, 1966. (Contractors report designation WAED 67.30E).
- b) Quarterly Technical Progress Report No. 2, SAN 679-2, December 1, 1966 to February 28, 1967. (Contractors report designation WAED 67.31E).
- c) Quarterly Technical Progress Report No. 3, SAN 679-3, March 1, to May 31, 1967. (Contractors report designation WAED 67.32E).
- d) Topical Report, Power Conditioning Parametric Screening Study, SAN 679-4, September 1967. (Contractors report designation WAED 67.45E).
- e) Topical Report, Parametric Design Study of Canned AC Induction Motors, SAN 679-5, January, 1968. (Contractors report designation WAED 67.52E).

*Task 2.4.3, Rotor Development, SNAP-50/SPUR Contract AF33(615)-2326

- f) Topical Report, Parametric Study of Inductor Alternators, SAN 679-6, February, 1968. (Contractors report designation WAED 68.01E).
- g) Topical Report, Experimental Verification, SAN 679-7 February, 1968. (Contractors report designation WAED 67.51E).

2. SYSTEM DEFINITIONS

2.1 POWER USES AND MISSIONS DEFINITIONS

2.1.1 Introduction

Several types of missions were investigated to determine the typical load requirements for space nuclear power systems. In order to specify the Power Uses and Missions, the power range, frequency and voltage requirements were evaluated as well as the relative amounts of ac and dc. The missions investigated were based on reference missions specified to be used as a guide by the contracting office⁽⁸⁾*. The specified missions were as follows:

- a) Interplanetary Mission (electrical propulsion).
 - 1) Unmanned Scientific Mission. One megawatt reference system to be based on table 5-1 of "Research on Spacecraft and Powerplant Integration Problems," Third and Fourth Quarterly Report, 26 October 1963 to 26 April 1964; G.E. Document 64SD700, Contract NAS3-2533.
 - 2) Manned Mars Mission. Eight megawatt reference system to be based on General Electric Document 66SD2026, "Study of Low Acceleration Space Transportation Systems"; Contract NAS8-11423.
- b) Manned Orbiting Space Station. Typical net power:
 - 1) AC--35 percent of total power--120 volts, 3-phase, 400 cps ± 1 percent.
 - 2) DC (Regulated)--40 percent of total power--28 volts ± 2 percent.
 - 3) DC (Unregulated)--25 percent of total power--30 to 40 volts.
- c) Lunar Base. Load types and division similar to the manned orbiting station can be used for the lunar base requirements.

The analysis of load requirements for the various orbital, interplanetary, and lunar base missions requiring electrical power of 0.25 to 10 megawatts determined that the majority of power is utilized in the form of dc, but that the relative percentages and voltage values are very much dependent upon mission objectives.

*Superscripts in parenthesis refer to references in section 5.

The characteristics of the major power users on each mission were also evaluated on an individual basis. In addition, the influence of the various mission loads on the system design and operation is discussed.

2.1.2 Scope of Evaluation

In order to provide adequate system evaluation in the design of 0.25 to 10 megawatt space nuclear electrical components, it is necessary to know the characteristics and magnitude of the loads to be supplied. It is also desirable to know the general vehicle configuration and the basic objectives of the mission. The number of missions of this type that are sufficiently defined to specify load requirements is rather limited. However, the mission evaluation reports that are available, by their diverse mission objectives, provide a good cross-section of the various missions for which such electrical power systems would be applicable. Each has been considered in as great a detail as possible to determine its effect on component selection.

Parameters evaluated included the power range, ac and dc requirements, frequency, voltage, launch and landing requirements, start-up requirements, and incident heat flux. The following load evaluations are based on detailed studies of the various mission analyses contained in published contract reports, references 9, 10, 11, 12, 13, 14 and 15. These included the missions specified by the contracting office. One aspect of the investigation that should be kept in mind is that several of the mission studies were based on initial assumptions concerning the power system which may not be valid for all missions of this type. For example, in some mission analyses, an alternator output voltage and/or frequency has been assumed at the start. This will naturally affect the input specifications for the loads and the system configuration. Similarly, assumptions of source or cooling system weight penalties will also affect the selection of the system configuration.

The loads analysis presented below includes a determination of user load requirements as well as system operational requirements. Of primary concern are the major loads which represent the greatest power users and therefore exert the greatest influence on selecting the system configuration. However, the system must still provide for supplying the required quality and quantity of power for those loads of lesser power demand; therefore, some loads are presented in generalized groupings, such as "communications" or "environmental control", so that the majority of these smaller loads may be provided for.

2.1.3 Mission Loads Determination

The following mission evaluations specify the types and ratings of loads for each mission that is considered. No attempt has been made to predict the load profiles or duty cycles of each of these loads and therefore the continuous load and possibly

the peak load will not be the sum of the individual loads. However, systems of this type are seldom loaded to 100 percent of their capacity and life and reliability generally increase as the continuous load percent of capacity decreases. Therefore, unless stated otherwise, all loads were assumed to be continuous.

A. Interplanetary Missions (electrical propulsion).

1. Unmanned Scientific Probe. In an unmanned scientific probe utilizing electric propulsion and having a power system capacity of 0.25 to 10.0 megawatts, the largest portion of the power by far will be used for the electric engines and for the radar and communications.

For such a mission there exist certain loads for guidance and control which remain relatively constant for varying mission objectives and total power demands. For a given mission, the loads demanding a large amount of power may often be programmed to limit the total instantaneous power demand to a value lower than their sum if all were to be operated simultaneously.

An analysis of loads from reference 9, for a typical spacecraft of one megawatt nominal load (1.4 megawatts maximum simultaneous load), resulted in the following maximum load values:

a)	Electric thrusters	
	878 kW	6600 volts dc
	12 kW	-6600 volts dc
	208 kW	20 volts dc
	4.5 kW	30 volts dc
	2.0 kW	28 volts dc
b)	Radar	
	154 kW	20000 volts dc
	2.0 kW	28 volts dc
c)	Communications	
	376 kW	20000 volts dc
	4.0 kW	28 volts dc
d)	Circulation pumps (electromagnetic)	
	75 kW	120 volts, 3-phase, 167 Hz
e)	Guidance and controls	
	8.5 kW	28 volts dc
	1.0 kW	26 volts, 2-phase, 400 Hz
f)	Generator excitation	
	10.0 kW	(at generated voltage)

It should be noted that, for this mission, the generator was initially assumed to supply three phase power at 120 volts, 2000 Hz. This no doubt affected the choice of the pump input voltage.

2. Manned Interplanetary Mission. A previous study(10) of manned missions to Mars has indicated that power levels of 4 to 10 megawatts will be needed depending upon spacecraft size and the allowable time to complete the trip and return to earth orbit. This range of capacity would provide a round-trip time of typically 600 days.

Aside from the larger vehicle size and resulting increased thrust requirements, the primary difference in the loads of this vehicle and those of the unmanned scientific probe is the need for a life support system. The life support systems for manned spacecraft of this type will normally require 0.5 to 2.0 kilowatts per man. Obviously, this represents only a small portion of the total generated power.

For the life support system, the majority of power is required for pumps, blowers, and oxygen regeneration. A typical division of power is 75 percent ac and 25 percent dc. The dc power is required at a nominal 28 volts to meet standard input requirements for the various items of equipment. The ac power is primarily used to drive motors and therefore it must be supplied at standard frequencies (or else requires the design of special motors to operate at the generated frequency) in order to eliminate the need for a multiplicity of frequency converters.

As a point of comparison to mission A.1. above, the radar, the communications, and the guidance and control loads can be expected to be of the same order for both missions. To these must be added the life support (hotel loads). The remaining power will be distributed to the electric thrusters with amounts supplied to the circulation pumps and generator excitation in proportion to the increased system capacity.

Assuming, as a basis for comparison, that all loads may occur simultaneously, the approximate division of power for a typical manned Mars mission in which 10 megawatts is supplied to the load would be as follows:

a)	Electric thrusters	8820 kW
b)	Radar	160 kW
c)	Communications	380 kW
d)	Guidance and controls	510 kW
e)	Generator excitation	105 kW
g)	Life support	15 kW

The voltage requirements of these loads would be quite similar to those for mission A.1.

B. Manned Orbiting Space Station. Several studies of power requirements for large manned orbiting space stations(11), (12) have indicated maximum peak loads of 20 to 40 kilowatts. However, these studies were predicated on a very early initial launch date prior to availability of the nuclear systems of much higher power levels. In addition, these missions were intended to serve primarily as laboratories to evaluate men and equipment in a space environment and therefore needed little more than the basic hotel,

communications, and control loads with some power allotted to experiments.

A recent study (13), however, evaluated several large manned orbiting space station configurations intended to serve in a more active capacity of mapping and communications than the orbital laboratory missions. This analysis included both a low orbit mission and a synchronous orbit mission. A summarization of requirements for these two missions is presented below.

1. Low-Orbit Space Station. The low-orbit space station would serve primarily for surveillance and mapping. Therefore, the major loads would be electric thrusters for orbital maneuvering (if required) and both active and passive electronic devices. Loads for a typical mission are as follows:

- a) Electric thruster (arc jets)
 - 240 kW at 130 volts dc (4 channels)
 - 4 kW at 120/208 volts, 3-phase, ac
- b) Radar
 - 28 kW at 15 kilovolts, dc
 - 12 kW at 10 kilovolts, dc
 - 14 kW at 5 kilovolts, dc
 - 14 kW at 28 volts, dc
 - 4 kW at 120/208 volts 3-phase, ac
- c) Communications
 - 14 kW at 8 kilovolts, dc
 - 2 kW at 5 kilovolts, dc
 - 1 kW at 2 kilovolts, dc
 - 1.5 kW at 1 kilovolts, dc
 - 1.5 kW at 28 volts, dc
- d) Circulation pumps (estimated value)
 - 25 kW at 3-phase, 120/208 volts, ac
- e) Guidance and Controls
 - 2.5 kW at 28 volts, dc
 - 2.5 kW at generated voltage ac (partially transformed and rectified internally)
- f) Generator excitation (estimated value)
 - 8 kW at generated voltage
- g) Life support
 - 1.8 kW at 28 volts, dc
 - 4.2 kW at 3-phase, 120/208 volts, ac
- h) Laser illuminator
 - 119 kW at 3 kilovolts, dc
 - 1 kW at generated voltage ac (transformed and rectified internally)

- i) Sensors and other loads
 - 30 kW at 10 kilovolts, dc
 - 5.0 kW at 120/208, 3-phase, ac
 - 11 kW at generated voltage ac (partially transformed rectified internally)

NOTE: Estimated values are based on system capacity and were not specified in the referenced study.

Had electric thrusters other than arc jets been used they would probably require dc power at a much higher voltage.

Since one assumption in this particular mission analysis was that the generated power was three-phase ac at 2040 volts, line-to-neutral, and 3200 Hz, the majority of loads utilizing the generated voltage and frequency provide internal transformation and rectification. The choice of three-phase ac power at 120/208 volts was made to permit the use of standard equipment. It appears likely however, that portions of power at this voltage will also be internally transformed and rectified by the equipment.

For this low-orbit mission, programming of loads could very likely be provided so that the maximum load was 300 to 350 kW rather than the arithmetic sum of all loads. Thus, the use of the thrusters for a period of time would coincide with reduced electronics activity.

2. Synchronous-Orbit Space Station. The synchronous-orbit manned space station is primarily a communications and surveillance mission with electric thrusters used for orbit-raising. The mission power requirements are somewhat similar to the low orbit mission except for the fact that there is increased communication capability and no laser illuminator or radar. Loads for typical mission are as follows:

- a) Electric Thrusters (arc jets)
 - 510 kW at 130 volts dc (6 channels)
 - 12 kW at 120/208 volts, 3-phase, ac
- b) Communications
 - 45 kW at 30 kilovolts, dc
 - 25 kW at 20 kilovolts, dc
 - 25 kW at 10 kilovolts, dc
 - 10 kW at 3 kilovolts, dc
 - 35 kW at generated ac (transformed and rectified internally)
- c) Circulation pumps (estimated value)
 - 41.5 kW at 3-phase, 120/208 volts ac
- d) Guidance and controls
 - 2.5 kW at 28 volts dc
 - 11.4 kW at generated ac (transformed and partially rectified internally)

- e) Generator excitation (estimated value)
12 kW at generated voltage
- f) Life support
1.8 kW at 28 volts dc
4.2 kW at 3-phase, 120/208 volts ac
- g) Sensors and other loads
30 kW at 10 kilovolt dc
6.2 kW at 120/208 volts, 3-phase ac
5.8 kW at generated ac (transformed
and rectified internally)

NOTE: Estimated values are based on system capacity and were not specified in the referenced study.

Those loads above which transform and rectify power internally normally utilize the power at a low value of dc voltage. For those loads not requiring a high dc voltage or at least only a portion of the power at high voltages, it is likely that all of the power could still be distributed at that voltage which, when rectified, would provide the major high-voltage load. A portion of this power would be transformed to the lower voltages at the load.

C. Lunar Base. It has been shown⁽¹⁴⁾ that a first-generation lunar base of six men or less will very likely not require a nuclear reactor as an energy source for the electric power system. However, as the base evolves and grows in size, the power demand will increase to the point where a nuclear electric power system provides the most practical approach.

The nuclear base differs from the other missions considered above in that there is no electric propulsion or surveillance-and-mapping equipment to require very large blocks of power at high kilovolt-level dc voltages.

The primary function of such a base is for exploration and scientific study. Although much of the power demand is in small blocks for more conventional loads, this power is needed for mobile or remote use. Typical uses are for vehicular propulsion, portable power sources, and remote shelter power. A study of lunar base power systems⁽¹⁵⁾ by Westinghouse showed that these power needs are best met by systems using hydrogen and oxygen as fuel. Naturally, the fuel supplies must be periodically replenished as they are used. Resupply from earth is extremely expensive for the required power levels and operating times and consideration must therefore be given to fuel regeneration.

The large nuclear power system can serve economically as a source of power for fuel regeneration through the electrolysis of water and the separation and liquefaction of the gases. Based on the growth of the base to a capacity of 18 men, fuel must be generated at a rate of 27 pounds of water processed per hour.

The Westinghouse study showed that the regeneration process required three kilowatt-hours per pound of water and that one electrolysis cell will operate at approximately 1.6 volts, 280 amperes. A typical electrolysis unit designed for the referenced study contained 65 cells in series to raise the value of required supply voltage. However, at least two of these units would be needed for the base with possible additional redundant units for reliability.

A second large potential user of power is for processing of the lunar soil, primarily in order to obtain water of which a large portion may be reprocessed for hydrogen and oxygen. Such a processing plant required 2 to 10 kilowatts per pound of water per hour depending (inversely) upon water content of the soil (1 to 10 percent).

The loads for a typical lunar base would be as follows:

- a) Life support
 - 9 kW at 28 volts, dc
 - 16 kW at 3-phase, 120/208 volts, ac
- b) Circulation pumps (estimated value)
 - 17.5 at 3-phase, 120/208 volts, ac
- c) Generator excitation (estimated value)
 - 6.8 kW at generated voltage
- d) Communications (estimated values)
 - 15 kW at 10 kilovolts, dc
 - 15 kW at 5 kilovolts, dc
 - 2 kW at 28 volts, dc
- e) Computers and controls (estimated values)
 - 3 kW at 28 volts, dc
 - 5 kW at generated voltage, ac (transformed and rectified internally)
- f) Fuel regeneration
 - 90 kW at 105 volts, dc
 - 15 kW at 3-phase, 120/208 volts, ac (pumps and compressors)
- g) Lunar soil processing
 - 150 kW at generated voltage, ac

NOTE: Estimated values are based on base size and power system capacity and were not specified in the referenced study.

The ac voltage of 120/208 volts for the fuel regeneration life support and circulation pump loads was again picked simply

as a standard value and could well be different for specially designed apparatus. Lunar soil processing equipment voltage and frequency requirements have not been determined and are therefore specified at the generated values.

In the processing of water into hydrogen and oxygen gas, electrostatic precipitators may be required for purifying the gases. These would require dc power at approximately 15 kilovolts; however, since only milliamps are needed, the total power would be quite low.

The referenced studies estimate that a total average load of 260 kilowatts would occur after several years of base evolution and that after seven years average power demand could reach 420 kilowatts with peaks up to 600 kilowatts.

To meet these load demands consideration was given to multiple launches of 100-kilowatt capacity and one year life. However, the use of larger power plants of up to 300 kilowatts and at least two years life could prove more economical. Redundancy or a second source of power would be needed in any case.

2.1.4 Mission Loads Evaluation

The loads discussed in section 2.1.3 represent typical requirements for the various missions. The specific load requirements are supplied directly by the generator or by power conditioning equipment such as transformers, rectifiers and frequency changers. Should the load requirements change slightly (as undoubtedly they will) the power conditioning designs will also change slightly. However, the selection of power system parametric data must be based on more generalized conditions. Thus, for example, the use of ion thrusters requiring 7.5 kilovolts rather than 6.5 kilovolts should have little, if any, effect on the selected system configuration outside of variations in the transformers and rectifiers.

This discussion is intended, therefore, to analyze the significance of the typical mission loads in the selection of the generalized system parameters.

A. Type of Power (AC or DC). Summarizing the power needs into arbitrary ac and dc grouping results in the following:

1. Interplanetary

- a) Unmanned scientific
 - 1420 kW kilovolt, dc
 - 229 kW low voltage, dc
 - 76 kW low voltage, low frequency ac
 - 10 kW generated voltage and frequency
(rectified internally)

- b) Manned interplanetary
 - 7654 kW kilovolts, dc
 - 1720 kW low voltage, dc
 - 522 kW low voltage, low frequency, ac
 - 105 kW generated voltage and frequency
(rectified internally)

2. Manned Space Stations

- a) Low orbit
 - 221 kW kilovolts, dc
 - 260 kW low voltage, dc
 - 20 kW generated voltage and frequency
(rectified internally)
 - 45 kW at low voltage and/or low frequency, ac
(partially rectified internally)
- b) Synchronous orbit
 - 135 kW kilovolts, dc
 - 514 kW low voltage, dc
 - 64 kW generated voltage and frequency
(rectified internally)
 - 64 kW at low voltage and/or low frequency, ac

3. Lunar Base

- 30 kW kilovolt, dc
- 104 kW low voltage, dc
- 48.4 kW at low voltage and/or low frequency, ac
- 11.8 kW at generated voltage ac (rectified)
- 150 kW at generated voltage ac

The kilovolt dc loads range between 1 and 30 kilovolts. Those loads requiring low voltage and low frequency ac are, for the most part, motors and electromagnetic pumps. Evaluation of their needs has been based on assuming standard voltages and frequencies such as 120/208 volts, 3-phase, 400 Hz. Many of these loads, if redesigned, could operate at the generated voltage and frequency which could prove to be advantageous for the larger loads.

By examining the load groupings it is seen that the majority of the power is supplied as dc and that much of the ac is transformed and/or rectified internally. Thus, the choice of generated frequency, waveform quality and even voltage in many cases has little effect on most of the loads.

The percentages of the various power forms for each mission are as follows:

	<u>DC</u>		<u>AC</u>	Used as AC
	<u>Kilovolt</u>	<u>Low Voltage</u>	<u>Internally Rectified</u>	
Unmanned Scientific	81.8%	13.2%	0.6%	4.4%
Manned Interplanetary	76.5	17.2	1.1	5.2
Low Orbit Station	40.0	48.1	3.7	8.2
Synchronous Orbit Station	17.4	66.2	8.2	8.2
Lunar Base	8.7	30.2	3.4	57.7

The relatively large amount of ac in the lunar base loads results from the fact that the form of power required for processing the lunar soil has not been specified. It is reasonable to expect that much of this power could also be dc.

It can be seen that for most missions, the dc loads represent over 80 percent of the total. For all but the lunar base, the relative amounts of low-voltage dc and high-voltage dc are primarily a function of whether arc-jets or ion thrusters are used for electrical propulsion. One might assume that a minimum weight system could be obtained by designing an alternator to provide the voltage needed to supply the largest dc load merely by rectifying without voltage transformation. Such an approach, however, prevents flexibility in system design; thus, a tradeoff analysis must be made to determine if such an approach is valid. Once hardware design is initiated, it becomes rather difficult to modify system parameters such as changing the generator voltage to meet a desired change in the type of electric thrusters that are used.

2.1.5 Individual Loads Evaluation

Since typical load values have been considered for each mission, it is necessary to consider the requirements and variations that may be expected for each of these loads. Only those loads that represent a significant portion of the overall power demand will be considered here.

A. Circulation Pumps. Pumps are required in the system for the various heat transfer and cooling loops. In most cases they are used to pump high-temperature liquid metal, although a low-temperature cooling loop of some other material is normally required for some load and power conditioning items.

The liquid metal pumps may be of either the electromagnetic or the centrifugal motor-driven type. A study(16) of

electromagnetic pumps has shown that electrical efficiency decreases with supply frequency and that above several hundred cycles per second, this decrease is quite rapid. Thus, the application of these pumps to the system will almost assuredly require frequency changers to provide output frequencies of the order of 50 to 200 Hz.

The selection of input voltage to the electromagnetic (EM) pumps is somewhat flexible. It may vary over a rather wide range with little variation in pump performance as long as the insulation system limit is not exceeded and the product of voltage and current remains essentially constant. Manufacturers of EM pumps have expressed a preference for voltages in the neighborhood of 100 to 200 volts. However, it appears likely that the design of the unit to operate from the generated or transmitted ac voltage would be desirable in order to eliminate the need for a transformer with the frequency converter.

The average efficiency for a typical electromagnetic pump is approximately 15 percent although 40 percent may be obtained for some applications. The effect of harmonic content on this efficiency has not been evaluated. Polyphase induction pumps are usually the best from an overall standpoint and exhibit weights of 94 to 130 lbs/kW.

The power factor of EM pumps is very low and may be 0.5 or lower. Most other loads on a given vehicle will be 0.7 or higher.

DC pumps require very low voltages (about 1 volt) along with high currents resulting in power conditioning efficiencies that make their use impractical for systems of this type.

The centrifugal pumps are normally driven by canned rotor induction motors. Frequency to these motors may be higher than for the EM pumps without as large a variation in efficiency. Frequency may cover a range of, for example, 200 to 1000 Hz depending on desired combinations of pump speeds and number of poles in the motor. Input voltages are also flexible depending on the maximum value for the insulation system. Typical motor efficiencies are 75 to 85 percent. This must be combined with the efficiency of the pump itself.

It is seen that the motor-driven pump may be operated directly from the generated ac or, if varying voltages or frequencies are advantageous from the standpoint of the motor design, frequency and/or voltage transformation may be necessary. Similarly, if higher-voltage transmission is used, a transformer may be needed at the motor.

In the study⁽⁹⁾ of a nominal one-megawatt system for the unmanned interplanetary probe, the pump motor input power requirements were found to be:

Reactor loop pump	60 kW
Primary radiator loop pump	42.5 kW
Boiler feed pump	16 kW

Shield radiator loop pump (optional)	5 kW
Auxiliary (generator, trans- former & motors) radiator loop pump	5 kW
High-temperature electronics cooling loop pump	5 kW
Low-temperature electronics cooling loop pump	5 kW

As system capacity increases, the individual motor power demands will also increase accordingly. When these pumps are supplied power from a frequency changer, it is also anticipated that, at least for the lower rating pumps, more than one pump will be supplied from a single frequency changer.

By extrapolating the pump ratings for a one-megawatt system to those needed for a 10-megawatt system it is seen that combined, total pump motor power demands may reach 300 to 600 kW. However, the largest single motor anticipated for a 10-megawatt system would require no more than 400 to 450 kW.

B. Electric Thrusters. For those vehicles requiring electric thrusters, primary consideration in past studies has been given to arc jets and ion engines. Although there are variations of each of these, it can generally be assumed that the majority of the power for arc jets will be at a relatively low dc voltage (in the order of 100 volts) while for the ion thrusters it will be at dc voltages in the order of 1 to 10 kilovolts.

In an ion engine, the beam and negative supply voltage are often chosen equal in magnitude and opposite in sign. The negative supply is desired to be voltage-regulated and current-limited in which case the beam supply voltage may be unregulated. The ionizer and neutralizer supplies are normally low dc voltage (20 to 100 volts).

The desired specific impulse of the contact and bombardment ion engines determines the relative values of the beam, the ionizer, and the accelerator power and voltage. For an efficient contact ion engine, the beam supply will be between 80 and 90 percent of the total engine power. The ionizer heater will take approximately 4 to 13 percent of the total power decreasing as the percent of beam power increases. Neutralizer and other loads take 5 to 7 percent of the power. Ion engine efficiencies range between 30 and 85 percent.

The required beam voltage varies widely with specific impulse. On a typical engine, for example, a specific impulse of 8000 seconds requiring a beam voltage of 4000 volts could be compared to the 7000 volts needed for an impulse of 10,600 seconds.

For the bombardment ion engine, beam power would represent 85 to 95 percent of the total engine load. Beam voltage is again in the order of kilovolts and varies with specific impulse. On some designs, it could exceed 20 kilovolts.

For many engine designs, heater loads could be supplied as ac. The SERT program⁽¹⁷⁾ engine designs have utilized the following voltages.

	<u>Beam</u>	<u>Accelerator</u>
Contact ion engine	5000 volts, dc	-2000 volts, dc
Bombardment ion engine	2500 volts, dc	-2000 volts, dc

Arc jets require voltages of 100 to 300 volts and although designs have been primarily dc, the use of ac may prove desirable. For ac designs, a relatively high frequency is needed to prevent ignition and extinction of arc. With the proper frequency, the ions will maintain a continuous arc; however, this frequency is estimated to be above 5000 Hz. The arc jets provide lower specific impulses than the ion thruster ($I_{sp} = 700$ to 3500 seconds) at an efficiency of 40 to 50 percent.

For most engine designs, the low voltage requirements, excluding the neutralizer, could be operated from ac rather than dc. This is especially significant for the arc supply in the arc jets and the ion heaters in the ion thrusters since these can represent a significant amount of power. Use of this power as ac may eliminate the additional weight and losses of the rectifier.

Since the choice of voltage for the beam and accelerator in the ion thrusters is dependent upon the desired specific impulse as well as engine development, it would appear somewhat impractical to specify a generator output to meet the needs of a single engine design; the chosen voltage should at least prove compatible with system designs utilizing other engines.

C. Radar and Communications. The radar and communications devices for these missions require primarily dc voltages of various levels. The majority of this power is required at high levels of 1 to 30 kilovolts. These voltages may be supplied directly to the equipment terminals or they may be obtained internally from an ac input. Although the amount of apparatus to condition the generated ac to usable dc values is essentially the same whether done internally or externally, the required dc values can greatly affect the system or configuration. Where these loads represent a large portion of the system capacity, high-voltage transmission to the loads at the required voltages may often prove desirable.

A much smaller portion of these loads is required at relatively low dc voltages and is often based on a 28-volt dc input as standard. Of course, these voltages may also be obtained from an ac input.

Because of the variations in types and objectives of the various radar and communications apparatus, no one or two kilovolt-level voltages may be assumed as standards. Efficiencies for the radar and communications devices are approximately 25 percent with most heat able to be removed from the transmitter at a relatively high temperature (typically 650°F).

D. Lasers. The technology of lasers has been advancing to the point where they will find use on future space missions in the areas of reconnaissance and communications. In the area of reconnaissance, they will serve for both illumination and radar applications.

The lasers also require dc voltages in the order of several kilovolts. As in the radar and communications applications, these voltages may be produced externally or internal to the laser power supply. Matching the transmitted voltage to the required laser voltage appears to be desirable from a system design standpoint when the laser represents a large power demand on the system.

For the low orbit space station, the laser illuminator required 120 kW. Laser communications, if used, would require up to 100 kW of power input. Increased laser radar or illumination capabilities on a typical low-orbit space station could increase the laser power demand to nearly a megawatt.

E. Fuel Regeneration. The regeneration of oxygen and hydrogen from water for use as fuel on the lunar base will require a large amount of dc power. As discussed previously, a typical electrolysis cell requires 1.6 volts, 280 amperes, dc. Over 100 of these individual cells would be required to meet the needs of an 18-man lunar base.

These cells would normally be connected in series in sufficient numbers to increase the supply voltage to a value (for example 100 volts dc) where the rectifier supply would have a high efficiency. However, to improve reliability, a series-parallel matrix arrangement may be desired whereby dc supply voltages as low as 40 volts and currents in the order of 1000 amperes may be needed.

F. Life Support. Because of standardized apparatus designs, the life support loads are generally specified to be 28 volts dc and 120/208 volts, 400 Hz, 3-phase ac. Most of the ac power is for driving induction motors. Development of these motors to meet some other (higher) supply frequency could permit the elimination of a frequency changer in some system designs; however, since these loads represent a relatively small portion of a typical mission capacity, the weight and losses of the frequency changer are less significant. Reliability should also be considered.

G. Generator Excitation. The amount of power required for excitation of the generator in a 0.25 to 10-megawatt system varies from about one percent of the generated power at the high ratings to about two percent of the power at the lower system capacities.

This power can be considered as supplied at the generated ac frequency and voltage since it would be rectified and controlled by the generator regulator.

2.1.6 Contributing System Factors

In addition to the various types of load equipment, there are a number of system factors that must be considered in selecting the power system configuration to meet the needs of a given mission. Just as the load equipment will affect the system selection, the needs of the selected system will often influence the operation of the loads. Some of these factors are considered below.

A. System Configuration. The system configuration chosen to meet the load requirements should provide minimum system weight while maintaining a high level of performance and reliability. Minimum system weight seldom results from designing individual components for minimum weight. The electrical system weight must include not only the generator, power conditioning and distribution weights, but also penalties for the reactor, turbine, and associated hardware as well as penalties to provide cooling for the various pieces of apparatus. Thus, a more efficient power conditioning system weighing more individually could result in reduced overall electrical system weight because of less losses and a resulting reduced weight penalty.

In selecting the system parameters, care must be taken to assure that the system is sufficiently flexible. For example, once hardware development is begun, changing the main parameters of the generator may be quite difficult without a major redesign effort. If the output voltage of the generator were matched to a particular load, the system may prove inflexible for use with other loads or in other missions. However, the change of a transformer output voltage level would be much simpler and more easily accomplished.

Designing for minimum system weight may also involve compromises in the design of a particular item in the system. For example, the use of a circulation pump operating at the generated frequency may eliminate any need for a frequency changer; likewise, the operation of a heater in an ion thruster on ac may save the weight and losses of a rectifier.

Although it requires a detailed analysis of the load designs when a mission has been closely defined, consideration should be given to operating the kilovolt loads at similar voltage levels to reduce the specific weight (lbs/kW) of transformers, rectifiers, contactors, and transmission lines. This is also true for selection of load frequencies other than the generated frequency.

Additional system considerations include the desirability of providing parallel systems for redundancy and increased initial

capacity and derating of equipment to meet the required life, reliability, and overloads.

B. Transmission Lines and Distribution Approaches. The selection of a distribution approach is dependent upon the load requirements, generator characteristics, and vehicle (or base) configuration. Power may be distributed to the load buses as:

- 1) Generated ac
- 2) High-voltage ac
- 3) High-voltage dc
- 4) High-voltage ac and high-voltage dc
- 5) Generated ac and high-voltage dc
- 6) Low voltage dc and one of the above

Transmission line weight and loss savings for higher voltages must be compared to the weight and losses of the transformers that may be needed to meet these voltage requirements. Transmission at the user-load voltage adds no additional penalty. Increased feeder size to reduce losses must also be considered.

For the higher-power, kilovolt-level dc loads there is also a choice on the location of the rectifier when transmitting at the load voltage. Transmission may be ac with rectification at the load or it may be as dc with rectification near the transformer. Generation of a relatively high value of voltage results in reduced losses in the feeders from the generator to the transformers. This provides greater flexibility in locating the transformers on the vehicle.

Transmission lines can be utilized for meeting specialized power conditioning requirements in certain systems. By proper selection of lengths, they may serve as an input inductance for frequency changers to limit the time rate of current rise. They may also aid somewhat in suppressing transients between power interruption devices and the load buses.

In evaluating the selection of transmission line parameters and their influence on the system, it is necessary to know the transmission lengths to be considered. An analysis of the proposed vehicle configurations for the missions discussed above results in the estimates of power transmission distances given below. The values of some of these distances for a given mission are based on similar requirements in the other missions.

1. Unmanned Interplanetary

- a) Generator to main contactors --- 4 feet.
- b) Main contactors to distribution panels and engine power conditioning --- 65 feet.
- c) Power conditioning to thrusters --- 5 feet.

2. Manned Interplanetary (8 megawatt configuration)

- a) Generator to main contactors --- 5 feet.
- b) Main contactors to distribution panels and engine power conditioning --- 150 feet.
- c) Distribution panel to cabin electronics --- 150 feet.
- d) Engine power conditioning to thrusters --- 10 feet.

3. Low Orbit Space Station

- a) Generator to main contactors --- 5 feet.
- b) Main contactors to distribution panels and cabin electronics --- 50 feet.
- c) Main contactors to engine power conditioning --- 100 feet.
- d) Engine power conditioning to thrusters - 5 feet.

4. Synchronous Orbit Space Station

- a) Generator to main contactors --- 5 feet.
- b) Main contactors to cabin electronics --- 175 feet.
- c) Main contactors to engine power conditioning --- 200 feet.
- d) Engine power conditioning to thrusters --- 5 feet.

5. Lunar Base

- a) Generator to main contactors --- 5 feet.
- b) Main contactors to user loads --- 1000 to 10000 feet.

In the study of the lunar base, specialized transmission lines were designed to meet the extremely long transmission distance in an adverse environment.

C. Load Tolerances. The tolerances of the loads to variations in voltage and frequency as well as waveshape can greatly influence the system design. This tolerance applies not only to the operational variations within a given system but also to the ability to operate in systems with differing nominal values. For example, can a given load designed for a 3300-volt input operate satisfactorily from an input of 3000 volts or 3500 volts. Such operation might permit operation of several loads at a common voltage or possibly even at the generated voltage. In general, this type of tolerance to varying voltages could only be determined by a detailed analysis of kilovolt level loads which is beyond the scope of this program.

Frequency regulation of ± 1 percent and voltage regulation of ± 3 percent appear to be values that can be anticipated for such a system and these are within the limits of MIL-STD-704 which is normally used for 400 Hz aircraft ac systems. Since fast-recovery diodes are required to meet the minimum considered frequency of 400 Hz, the choice of a particular generated frequency should not affect the selection of the rectifier components. Also, because the majority of the power is rectified, close control of the frequency may not even be essential, especially if the critical ac loads are supplied through a frequency changer.

Because of the low efficiency of many of the high-voltage loads such as radar and communications, methods of improving these efficiencies should be investigated in future designs. This might be accomplished by providing even closer regulation of the bus voltage. Of course, any increase in efficiency must be compared to the added system complexity to assure this tolerance.

Distortion of the generated waveshape and its effect on the loads must also be considered when a large amount of the system capacity is transformed and rectified. In a system where cycloconverter frequency changers are utilized, this can affect the ability of the frequency changers to produce a satisfactory output waveform.

D. Transients and Faults. System transients must be given close consideration in selecting parameters and equipment designs for the power systems. These transients will serve to determine the peak-inverse voltage ratings of the semiconductor power handling devices such as the rectifiers. The proposed revision of MIL-STD-704 sets transient voltage limitations for three-phase, 115-volts, 400 Hz aircraft systems as follows:

- 1) For 0.85 per unit load with 90 percent interrupted, maximum voltage is 140 volts for 0.02 seconds (122 percent).
- 2) For 1.7 per unit load with 90 percent interrupted maximum voltage is 160 volts for 0.04 seconds (139 percent).
- 3) For fault removal, maximum voltage is 180 volts for 0.1 seconds (157 percent).

The above values are based on experience with systems utilizing salient pole alternators. For the inductor alternators such as would be used in a high-speed, high-temperature nuclear system, indications are that the comparable generated transients may be as much as 50 percent higher because of differences in internal reactances.

Selection of semiconductors to meet these transient voltages requires a significant derating and for the power handling devices this may increase the equipment overall size considerably.

For the 28-volt dc bus on aircraft, the proposed MIL-STD-704 revision permits transients that are relatively greater than for the ac bus. Specified values are:

- 1) For 0.85 per unit load with 90 percent interrupted, the maximum voltage is 60 volts for 0.015 seconds (214 percent).
- 2) For 1.7 per unit load with 90 percent interrupted, the maximum voltage is 70 volts for 0.02 seconds (250 percent).
- 3) For fault removal the maximum voltage is 80 volts for 0.05 seconds (286 percent).

The MIL-STD-704 transient values are based on the fact that normal loading is between 15 and 85 percent of nominal capacity and that the minimum power factor is 0.85 lagging. In the existing specification, negative transients and voltage spikes of very short duration are not covered although these may present problems in some systems.

In order to operate satisfactorily in an electrical system meeting MIL-STD-704, much of the equipment is designed to even more stringent requirements. For example, newer equipment designs⁽¹⁸⁾ for operation from a 28-volt bus will often be designed to meet transient voltages of +100 and -40 volts. Some of the equipment is being designed to meet spikes of up to 600 volts for 1 to 10 microseconds; this is quite difficult for the power handling equipment to do.

Most ac bus loads are either less susceptible to damage from transient voltages or are easily filtered for protection. Therefore, the dc loads are the most critical in the area of transient-voltage induced failures. There is usually a trade-off between suppressing transients versus making the load equipment more tolerant of voltage variations. For power handling equipment, either approach is likely to add considerable weight. There is a third approach which is a combination of suppression and tolerance that is quite often the best solution.

In many system designs, the transmission lines will provide suppression of the worst spikes. A generalization has also been made that items that are sensitive to transients do not often generate them. Thus, isolating the two types of equipment on separate buses will provide a certain amount of protection by means of the interconnecting transmission lines and power conditioning apparatus.

For purposes of comparative evaluation, the MIL-STD-704 relative level of maximum transients (160 percent) should be used. However, a much more detailed analysis and evaluation should be made for each system considered. At the higher voltage levels the use of this percentage results in a wide swing in actual voltage levels.

Frequency transients are also of concern but are less likely to result in damage to the system. MIL-STD-704 allows a

maximum 500 Hz frequency transient for fault switching in 400 Hz systems.

Ability to meet overload and fault requirements is also desirable in the power system. Rather than have interruption of power by a circuit breaker with only a slight overload, this equipment should permit supplying a specified overload level for a given period of time to permit the fault to clear or the overload transient to pass. This of course will increase the size of the power conditioning equipment but provides the only practical basis of system comparison. Typical current overload capabilities for power conditioning design are 300 percent for up to 5 seconds. Final selection of overload capabilities are based on mission and system characteristics. If non-interruption of loads is critical, a high value of overload would be necessary.

2.2 NUCLEAR MECHANICAL SYSTEMS DEFINITIONS

2.2.1 Introduction

In order to evaluate and optimize each of the various electrical system components relative to the others, at least one common design parameter must be chosen as a basis for comparison. For space power systems, one of the more important parameters is weight, both the physical weight of the components and the weight penalty attached to its less than 100 percent efficient operation. This weight penalty consists of two parts, that of the power system supplying make-up power to account for the losses and that of the cooling system to reject the losses. This means that, in order to evaluate each electrical component individually for minimum weight, the specific weight* of all of the power supply system (and components therein) must be known as well as the specific weights of the heat rejection system and components.

A part of the present study has been to determine the approximate weights of the various powerplant components. This was accomplished by reviewing the work of past conceptual system design studies** and either scaling the powerplant weights shown to coincide with the four ratings of the design study or calculating new weights based on new designs. Because of the lack of large rating (several megawatt) conceptual designs in the previous studies reviewed, considerable scaling and analyses had to be done on the 5 and 10 megawatt ratings.

The tabulation of the individual component weights which were determined are listed in table 2.2-1 for the four basic ratings at 500°, 800°, and 1100°F coolant temperature. Details of how the various weights were determined follow. A few words of explanation are in order first however.

*Usually specified in a convenient form as pounds per kilowatt of power supplied or pounds per kilowatt of heat rejected.

**References 9, 10, 13, 14, 20, 21, 22, 23, 24, and 25.

Table 2.2-1. Powerplant Component Weights

Component	Rating Temp. °F	0.35 MWe			1.0 MWe			5.0 MWe			10.0 MWe		
		500	800	1100	500	800	1100	500	800	1100	500	800	1100
1. Reactor - 10,000 hr. life	Units												
40,000 hr. life	lbs	← 1300 →			← 3390 →			← 5460 →			← 10890 →		
	lbs	← 1618 →			← 4370 →			← 21700 →			← 43600 →		
2. Boiler	lbs	← 97 →			← 277 →			← 1385 →			← 2770 →		
3. Condenser	lbs	← 63 →			← 180 →			← 901 →			← 1802 →		
4. Reactor Motor Heat Exchanger	lbs	Assumed to be an integral part of the motor											
5. Boiler Feed Pump	lbs	← 29 →			← 48 →			← 125 →			← 217 →		
6. Primary Radiator Pump	lbs	22 25 23			38 41 40			144 152 154			290 286 284		
7. Reactor Coolant Pump	lbs	← 15 →			← 21 →			← 86 →			← 160 →		
8. Primary Radiator	lbs	← 1500 →			← 4291 →			← 21450 →			← 42910 →		
9. Turbine	lbs	27 36 49			130 156 260			1576 1576 2740			2664 4000 5330		
10. Misc Powerplant	lbs	← 53 →			← 151 →			← 756 →			← 1512 →		
11. Misc Power Conversion System	lbs	← 93 →			← 236 →			← 1013 →			← 1853 →		
12. Subtotal - 10,000 hr.	lbs	3189	3201	3212	8736	8765	8868	32880	32888	34054	65068	66400	67728
40,000 hr.	lbs	3507	3519	3530	9716	9745	9848	49120	49128	50294	97778	99110	100438
13. Generator	lbs	294	332	490	1008	1151	1749	5094	5318	10644	9769	14064	25977
14. Specific Weight (S _e)-10,000 hr.	lb/kwe	9.97	10.09	10.59	9.74	9.92	10.62	7.60	7.64	8.94	7.48	7.62	9.37
40,000 hr.	lb/kwe	10.86	11.00	11.49	10.72	10.90	11.60	10.84	10.89	12.18	10.18	11.32	12.64
15. Auxiliary Radiator and Heat Exchanger	lbs	605	207	87	1420	480	207	6200	2100	896	12640	4280	1560
16. Specific Weight (S _L)	lb/kw	11.8	4.0	1.7	11.8	4.0	1.7	11.8	4.0	1.7	11.8	4.0	1.7
17. Secondary Radiator	lbs	← 378 →			← 953 →			← 6600 →			← 12600 →		
18. Secondary Coolant Pump	lbs	← 13 →			← 16 →			← 34 →			← 46 →		
19. Specific Weight (S _L)	lb/kw	← 44 →			← 44 →			← 51 →			← 51 →		
20. Shield - 10,000 hr. unmanned	lbs	← 688 →			← 1560 →			← 2912 →			← 5790 →		
40,000 hr. unmanned	lbs	← 5350 →			← 8760 →			← 13400 →			← 2250 →		
40,000 hr. unmanned	lbs	← 1021 →			← 2436 →			← 12860 →			← 25240 →		
40,000 hr. manned	lbs	← 5690 →			← 9710 →			← 35600 →			← 61000 →		
21. Total System - 10,000 hr. unmanned	lbs	5173	4819	4868	13693	12925	13353	53720	49852	55340	105913	98885	113701
40,000 hr. unmanned	lb/kwe	14.8	13.7	13.9	13.7	12.9	13.4	10.7	10.0	11.1	10.6	9.9	11.4
40,000 hr. manned	lbs	9835	9481	9530	20893	20125	20553	64208	60340	65628	122623	115595	130411
40,000 hr. unmanned	lb/kwe	28.0	27.0	28.2	20.9	20.1	20.6	12.8	12.1	13.1	12.3	11.6	13.0
40,000 hr. unmanned	lbs	5818	5470	5519	15549	14781	15209	79908	76040	81328	152368	155340	165861
40,000 hr. manned	lb/kwe	16.6	15.6	15.8	15.6	14.8	15.2	16.0	15.2	16.2	15.2	15.5	16.6
40,000 hr. manned	lbs	10487	10139	10188	22823	22053	22443	111648	98780	104068	188128	191100	201621
40,000 hr. manned	lb/kwe	30.0	29.0	29.1	22.8	22.1	22.4	22.3	19.8	20.8	18.8	19.1	20.2

The auxiliary radiator cooling system does not list a separate auxiliary radiator pump. The pump to circulate the auxiliary coolant flow is an integral part of the primary radiator pump. The secondary radiator system contains organic fluid and operates at an average temperature of 175°F, not the 500°, 800°, or 1100°F implied by the table; they are listed as such to show their relationship with the various electrical ratings irrespective of the 500° to 1100°F coolant temperatures. The radiator weights are based on estimated losses as determined by this study, the Power Uses and Missions study and the Motor and Generator Parametric Screening studies. Also, the component weights are listed as if for a single system without redundancy.

The turbine and generator weights are based on design speeds according to near optimum generator speeds listed in section 3.1. In selecting the speed, it was assumed that the turbines could operate at the selected values. Generator and pump motor with weights are according to the power requirements specified and the parametric screening studies data. Pump weights by themselves were estimated at 8 lbs/MWe (system) plus 50 percent more for structure to motor. The shield weights are based on the following parameters:

- Manned - 1.5 mrem/hour
 - 60 ft. diameter dose plane
 - 150 ft. separation distance
- Unmanned - 3×10^{12} nvt (combined equivalent dose)
 - 25 ft. diameter dose plane
 - 50 ft. separation distance

To adjust the shield weights for other separation distances, the weights must be multiplied by the correction factors given in the following sections.

The specific weights for the turbine shaft power (S_g in section 2.2.6) are not summarized in the table 2.2-1; they are presented in figure 2.2-1. Examination of the data reveals that the specific weight at 40,000 hours is approximately 1.1 to 1.5 times that at 10,000 hours. Likewise, the specific weight of the total system, as summarized in the following, is heavier at 40,000 hours.

Table 2.2-2 Average Ratio of Total System Specific Weights at 40,000 and 10,000 hours

kW rating	350	1000	5000	10,000
Manned	1.06	1.10	1.69	1.60
Unmanned	1.14	1.15	1.40	1.39

Figure 2.2-1. Turbine Shaft Power Specific Weight

Notice that, as the coolant temperature increases from 500° to 800° to 1100°F for the various ratings, the auxiliary or coolant radiator and heat exchanger weight decreases an amount less than the turbine and generator weight increase. The net result is that the lightest systems - least specific weight occur at the 800°F electrical system coolant condition rather than at 1100° or 500°F. This is indicative of the loss in electrical performance per pound of electrical system that usually occurs at the higher temperatures.

2.2.2 Scope of Evaluation

The object of this task was to provide data on the nuclear and mechanical conversion systems in order to enable the electrical designer to select those generator and other electrical component designs which are appropriate to a fully optimized nuclear to electrical conversion system and to determine the characteristics of such systems.

The study was limited to Rankine cycle conversion systems such as could be expected to be available around 1980. The components of the systems which have been considered are those which are important in virtue of their weight, size, efficiency, electrical power consumption, or nuclear radiation. Component design and performance data was obtained in parametric form, where required, at the conditions appropriate to the requirements of the electrical components.

Because the results of this study are to be incorporated into electrical components which are likely to be in service around the 1980 time period, the component and system technology utilized in the design of the nuclear and conversion system components has been an advanced one typified by the extensive use of refractory metals in the construction of the equipment. Many studies have been made of the temperature capabilities of refractory metal systems. These can be summarized by saying that with Columbium base alloys reactor outlet temperatures of about 1800° to 2000°F can be expected while use of Tantalum and Tungsten base alloys will permit reactor outlet temperatures about 300° to 500° higher than this. It is expected that Molybdenum and other refractory metal turbine alloys will be available as they are needed for use in association with these high-temperature reactors.

Based upon these considerations, a cycle was devised on NASA contract NAS5-250 around this state-of-the-art and is shown in figure 2.2-2. The radiator temperature of this cycle was selected based upon the results of numerous previous studies.

2.2.3 Nuclear Systems

A. Reactor. Reactor weights are required in order to compute power plant weight. Reactor dimensions are necessary in order to calculate shielding weights. This data was required for

Figure 2.2-2. Flow Diagram for Rankine Cycle with Liquid Coolant Radiator

four reactor powers 0.35, 1.0, 5.0, and 10.0 MW electrical and for 10,000 and 40,000 hours life. Dimensions and weights were estimated for these eight reactors by extrapolation from available reactor designs. Three reactor designs in this power and lifetime range and utilizing refractory metal construction and high-temperature fuel materials were available from a previous study entitled "High Temperature Liquid Metal Cooled Reactor Study" Contract NASw-673 and these were used as a basis for extrapolation.

The method of extrapolation used was dependent upon the reactor sizes considered. The small reactors are largely criticality limited whereas the large ones are burnup limited and also have diameter limitations resulting from the reduced effectiveness of the reflector control with increasing core diameter.

The interpolated and extrapolated reactor dimensions were used to calculate reactor volumes and, together with a mean density, to calculate reactor weights. Table 2.2-3 lists the salient dimensions and some derived quantities for the base-line reactors of the above referenced study. Table 2.2-4 lists the extrapolated and interpolated dimensions for the reactors of this study.

A survey of reactor weights indicated that, approximately,

$$\frac{\text{Reactor Weight, lb.}}{\text{Core Volume, Cu. In.}} = 0.7$$

and this was used to calculate reactor weights from the core volumes.

Table 2.2-3. NASw-673 Reactor Designs

M W Thermal	2	8	33
Core Diameter, in.	14.4	20.0	22.0
Core Length, in.	14.4	40.5	54.0
Core Volume, Cu. In.	2,350	12,700	20,500
Life Hours	10,000	20,000	20,000
M W Hours/Cu. In.	8.51	12.60	32.2
Weight	1256	5313	14,678

Table 2.2-4. Reactor Core Dimensions

MW Electrical	0.35	1.0	5.0	10.0
MW Thermal	1.75	5.0	25.0	50.0
<u>10,000 Hour Life</u>				
Diameter, in.	13.0	18.0	21.0	22.0
Length, in.	14.0	19.0	22.5	41.0
Core Volume, Cu. in.	1,856	4,840	7,800	15,550
Reactor Weight, lb.	1,300	3,390	5,460	10,890
M.W.T. Hours/cu. in.	1.89	2.01	6.41	6.43
<u>40,000 Hour Life</u>				
Diameter, in.	14.0	19.0	22.2	28.0
Length, in.	15.0	22.0	80.0	101.0
Core Volume, Cu. in.	2,313	6,240	31,000	62,300
Reactor Weight, lb.	1,618	4,370	21,700	43,600
M.W.T. Hours/cu. in.	6.05	6.41	6.45	6.42

B. Nuclear Radiation Shield. Many parametric studies have been made of nuclear radiation shields, e.g., reference 19; however, they do not cover reactor sizes as large as the ones of this study so that it was necessary to augment them by designing additional nuclear shields for both manned and unmanned applications.

The shield designs were of the usual lithium hydride-depleted uranium type and are split by two foot wide gallery to accommodate primary loop components such as heat exchangers, expansion compensators, pumps, etc. The general design conditions were selected to agree with those of reference 19.

1. Manned Applications. A dose plane diameter of 60 feet was assumed together with a dose plane to reactor distance of 150 feet. The design combined equivalent dose rate is 1.5 mrem per hour with RBE factor of 5. The resulting manned shield weights are given in table 2.2-1. The correction factor to adjust shield weight for varying reactor to dose plane distance is plotted in figure 2.2-3.

Figure 2.2-3. Manned Vehicle Shield Weights

2. Unmanned Applications. A dose plane diameter of 25 feet situated 50 feet from the reactor was assumed together with a combined equivalent dose rate of 3×10^{12} nvt. The resulting unmanned shield weights are given in table 2.2-1.

The correction factor to adjust shield weight for varying reactor to dose plane distance is plotted in figure 2.2-4.

2.2.4 Conversion Systems

A. Turbines. Turbine weights and rotor polar moments of inertia were required for a family of turbines having a range of speeds and powers and designed to match the generators being considered in this study.

The method adopted to get this data was to take a specific turbine designed to meet conditions at a point inside the range to be considered, calculate its weight and polar moment and scale these quantities to the conditions required to match the generator designs. The potassium turbine chosen as the base was a six-stage unit designed on contract NAS7-390 "Basic Studies of Turbine Blade Erosion Phenomena".

The polar moment of inertia of the rotor was calculated by integrating radially the blading, discs, etc; that is,

$$I = \int \rho A r^2 dr$$

where ρ is the material density and A is the cross section by circular cylinders coaxial with the rotor. The result was 169 lbs in². The weight of the rotor and stationery parts were calculated from the volume to be 120 pounds. The specifications of this reference turbine are given in table 2.2-5.

The method used for scaling to other speeds and powers was to first scale all dimensions of the reference design by a constant factor to the desired power level. The constant factor is the square root of the ratio of the power required to the power of the reference design (0.9 MWe). It was then assumed that as the rotational speed varied, the blade speed would re-

main unchanged so that the diameter (D) $\sim \frac{1}{N}$. Also, it was assumed

that the blade aspect ratio would remain constant so that the length was proportional to the blade height and that the annulus flow area would remain constant. Consequently;

$$L \sim h \sim \frac{1}{D} \sim N$$

Hence, approximately, $D^2 L$ and the weight are proportional to the reciprocal of the speed. The weights calculated by this method are given in table 2.2-6.

In order to estimate the variation of the polar moment of inertia of the turbine rotor with power level and speed, it was assumed that the blade speed would remain approximately constant

Figure 2.2-4. Unmanned Vehicle Shield Weights

Table 2.2-5. Reference Turbine Design

Power	0.9	MWe
Speed	24,000	rpm
Weight	120	lb
Rotor Polar Moment of Inertia	169	lb. in ²

Table 2.2-6. Turbine Weights

Power (MWE)	Coolant Temperature (°F)	Turbine Alternator Speed (RPM)	Turbine Weight (lbs)	Turbine Polar Moment of Inertia (lbs in ²)
0.35	500	40,000	27	15.4
	800	30,000	36	20.5
	1100	22,500	49	27.4
1.0	500	24,000	130	209
	800	20,000	156	251
	1100	12,000	260	418
5.0	500,800	12,000	1567	10460
	1100	6,857	2740	18300
10.0	500	12,000	2664	41800
	800	8,000	4000	62700
	1100	6,000	5330	83600

and that at fixed rotational speed the blade thickness was proportional to the blade height which was in turn proportional to the power level. Hence, at constant rotational speed, the moment of inertia was assumed proportional to the square of the power.

At fixed power, blade thickness was assumed proportional to blade height which was assumed inversely proportional to the diameter and hence directly proportional to the speed. Consequently, at fixed power, the moment of inertia was assumed inversely proportional to the first power of the rotational speed.

These scaling assumptions yield an inertia variation with linear scaling proportional to the fifth power of the scale factor. The resulting rotational moments are tabulated in table 2.2-6. Because of the assumption that blade speed would remain unchanged, the turbine stresses will remain essentially unchanged and thus, the speeds will be compatible with the desired alternator speeds. This may not result in an optimum turbine design and speed but for purposes of this study was considered adequate; however, this approach does not permit direct comparison between generator limiting speeds and turbine limiting speeds in as much as the turbine would have no separate limiting speed.

B. Primary Radiators. The radiator designs are based upon the work in WANL-PR-(B)-009, (20) the final report on contract NAS5-250.

From the cycle diagram figure 2.2-2, the radiator inlet temperature is seen to be 1390°F and the outlet temperature 1365°F. The radiator fluid is lithium which is contained in a columbium base alloy tube 0.025-inch thick. The armoring material used is beryllium. The armor thickness was calculated as follows using a reliability of 0.99 for 17,600 hours. The required armor thickness may be expressed in terms of the meteoroid flux, the mission design parameters, the target material properties, and the isotopic flux impact assumption. The required armor thickness is given by:

$$t = \frac{a}{2.54} \gamma \left(\frac{6}{\pi} \right)^{1/3} \rho_p^{-1/3} \left(\frac{62.4 \rho_p}{\rho t} \right)^{\phi} \left(\frac{v}{c} \right)^{\theta} \left(\frac{a A_r}{-\ln P(0)} \right)^{1/3\beta} \left(\frac{2}{3n\theta\beta + 2} \right)^{1/3\beta}$$

where

t = Required single material armor thickness in inches

a = Thin plate and spall adjustment, 1.75

$\gamma = 2$

ρ_p = Meteoroid particle density, 0.6 gm/cm³

ρ_t = Radiator (target) material density, lbs/ft³

ϕ = 1/2

$\bar{v}$ = Average meteoroid velocity, 98,400 ft/sec (30km/sec)

$$c = 12 \sqrt{\frac{E_t g}{\rho_t}}$$

where E_t is Young's modulus at radiator operating temperature, lbs/in², and g is 32.2 ft/sec².

θ = 2/3

α = 1.01 x 10⁻¹⁰

β = 1.34

A = External surface area of fluid passages of radiators (outer circumference of tubes), ft²

τ = Mission time in days

$P(o)$ = Design probability of no critical damage to radiator system

n = 1 (valid for oblique penetration dependent upon the normal component of velocity).

A cylindrical radiator configuration was selected in order to make weight and area estimates; this is the most likely configuration to be used because it conforms to the launch vehicle geometry and does not require deploying. Additionally, cylindrical radiators are structurally superior to flat radiators for large power ratings. For the radiators covered by this study, this is important. The structure accounts for a major part of the radiator weight.

Based upon the above referenced studies, a specific weight of 3.63 pounds per square foot of radiator surface (including structure, micrometeoroid armor and fluid inventory) was used for all radiators. Although some variation of specific weight with radiator size will occur, it will not be substantial in this size range. For the radiator inlet and outlet temperatures of the cycle diagram, figure 2.2-2, optimization of the radiator geometry has shown that a primary radiator area of about 1,310 square feet per megawatt is required. Radiator weights and areas are shown in table 2.2-7.

Table 2.2-7. Primary Radiator Area & Weight

Electrical Power, MWe	0.35	1.0	5.0	10.0
Radiator Area, sq. ft.	459	1310	6550	13,100
Radiator Weight, lbs.	1500	4291	21,450	42,910

C. Boiler Weights. The weights of the boilers were estimated from the boiler designs contained in report WANL-PR-(B)-009(20). The boiler designs contained in that report were for the same thermodynamic cycle conditions as we have here and were based upon a very extensive review of the literature. In a tube and shell boiler design, each tube is designed for a definite flow; consequently, varying the design flow in a boiler while maintaining the same inlet and outlet thermodynamic conditions only changes the number of boiler tubes. The weight of the boiler will change both because of the change in the number of boiler tubes and because of the change in the headers and tube sheets. Because the boiler operates at high temperatures and pressures, the stress conditions in the headers are onerous and if the design was simply increased in size the thickness would have to be increased and the weight would increase more than linearly with output. In order to avoid this the headers must not be increased in size, either by subdividing them or if necessary by employing a multiplicity of boilers for the larger output plants. If this is done, the boiler weight can be expected to vary linearly with output. This assumption was made in calculating the boiler weights which are presented in table 2.2-1.

D. Condenser Weights. Similar reasoning to that employed in the case of the boiler leads to the conclusion that condenser weight will also vary linearly with system electrical output in the power range under consideration. Because the pressures in the condenser are low, the weight of the headers will be closely proportional to the number of tubes. Because the thermodynamic cycle conditions were chosen to be the same as those employed in report WANL-PR-(B)-009(20), the condenser design for the potassium working fluid reported therein was scaled linearly with plant output to obtain the condenser weights listed in table 2.2-1.

E. Primary Radiator Loop Pump. This power was calculated from a radiator design code. NaK was used as the coolant fluid and the designs generally conformed to the radiator design

in report WANL-PR-(B)-009 previously referred to. A pump efficiency of 80 percent was assumed to calculate the mechanical power requirements. Pump powers are listed in table 2.2-8.

Table 2.2-8. Primary Radiator Loop Pump Power

System Electrical Power, MWe	0.35	1	5	10
Pump Mechanical Power, hp	5.9	18.5	115	270

F. Expansion Compensators. In fully high temperature liquid metal loops, the difference in expansion between the contained liquid and the tubing, etc., can be large. In addition, expansion compensators are relatively heavy so that they are important equipment items. A design investigation has shown that about 225 lbs per cubic foot of expansion volume is a reasonable design value.

1. Reactor Loop. The volume to be compensated for was calculated assuming a total of 22.5 ft. of piping and a reactor $\Delta T = 150^\circ\text{F}$. The lithium coolant expands 20 percent between hot and cold conditions. An additional, approximately equal, expansion volume was allowed for the reactor and boiler. The calculated compensator weights are given in table 2.2-9.

Table 2.2-9. Primary Loop Expansion Compensator Weights

Power, MWe	0.35	1	5	10
Expansion Compensator, Weight, lb.	53	151	756	1512

2. Radiator Loop. A design was done for the 1.0 MWe system. This was then scaled by taking the radiator fluid volume to be proportional to the three halves power of the power level for other ratings. Expansion was allowed for between the freezing point of NaK and 1150°F ; 45 percent additional volume was calculated to be a suitable allowance for the condenser and piping connecting the radiator to the condenser. The compensator weights are tabulated in table 2.2-10.

Table 2.2-10. Radiator Loop Expansion Compensator Weights

Power, MWe	0.35	1	5	10
Expansion Compensator Weight, lb.	93	236	1013	1853

G. Reactor Coolant Pump. In calculating the reactor coolant loop power, the coolant flow was calculated using a reactor $\Delta T = 150^\circ\text{F}$ and a lithium coolant. A survey of the literature on reactor designs indicated that the reactor loop pressure drop will be about 10 psi. The pump efficiency was 74 percent except for the 0.35 MWe plant where it was 61 percent because of the small size. The reactor coolant pump powers are tabulated in table 2.2-11.

Table 2.2-11. Reactor Coolant Pump Powers

Power, MWe	0.35	1	5	10
Reactor Coolant Pump Power, H.P.	1.75	4.0	20	40

H. Boiler Feed Pump. The boiler feed pump power was calculated using a 1.0 MWe system boiler feed pump as a reference⁽²⁰⁾ and extrapolating to the other power levels. The boiler feed pump powers are tabulated in table 2.2-12.

Table 2.2-12. Boiler Feed Pump Powers

Power, MWe	0.35	1	5	10
Boiler Feed Pump Power, H.P.	4.6	13	65	130

2.2.5 Auxiliary and Secondary Radiators For Electric Equipment

In addition to normal fin and tube radiators, vapor chamber radiators were investigated for this application. The formula

for the thickness of the chamber surface required to give a reliability against meteoroid penetration of P_0 is given by

$$t = \text{Constant} \times \left(\frac{A}{-\ln P} \right)^{\frac{1}{4.02} \tau}$$

where A is the vulnerable area and τ the time of exposure. This is deduced from the formula given in the previous section on primary radiators. It is desirable to minimize the radiator weight which is proportional to $n A_c t$, where n is the number of vapor chambers and A_c is the vulnerable (and therefore protected) area of each chamber. This is to be minimized subject to the radiant area available at the end of the mission, $n A_c P$, being constant and equal to A_r , the required radiator area at the end of the mission. The smaller A_c the better since then only a very small part of the radiator is lost on each penetration. We therefore fix A_c at the smallest practical value (bearing in mind also that the total chamber wall weight will increase with increasing number of chambers.)

We have

$$n A_c t = \text{Constant} \times n A_c \left[\frac{A_c \tau}{-\ln \left(\frac{A_r}{n A_c} \right)} \right]^{\frac{1}{4.02}} \sim \left[\frac{1}{-\ln \frac{A_r}{n A_c}} \right]^{\frac{1}{4.02}}$$

Differentiating to find a minimum we have

$$\begin{aligned} \frac{d(n A_c t)}{d n} &\sim \frac{d}{d n} n \left[\frac{1}{-\ln \frac{A_r}{n A_c}} \right]^{\frac{1}{4.02}} \\ &= -\frac{n}{4.02} \left[\frac{1}{-\ln \frac{A_r}{n A_c}} \right]^{\frac{5.02}{4.02}} \cdot \frac{n A_c}{A_r} \cdot \frac{A_r}{A_c} \cdot \frac{1}{n^2} = 0 \end{aligned}$$

Therefore,

$$\ln \frac{A_r}{A_c} = - \frac{1}{4.02} \quad \text{and} \quad n A_c = A_r / 0.78$$

Thus, a vapor chamber radiator can be expected to equal the area of a fin and tube radiator of around 0.78 effectiveness. This is similar to the values found for typical tube and fin radiators. In order to calculate the radiator areas required an emissivity of 0.9 and an effectiveness of 0.8 was used. The estimated electrical losses (cooling loads) that the radiators must handle, plus the corresponding coolant flow requirements, are shown in table 2.2-13 and table 2.2-14 respectively. The generator losses were estimated from the calculated losses on a previous study(26) on contract NAS5-1234 carried out by Westinghouse. The other losses were estimated using data of reference 9 and extrapolating using the following assumptions (see figures 2.2-5, -6, -7 and -8 for extrapolated systems):

- a) Missions for all four ratings considered are unmanned scientific space probes with similar type loads.
- b) Generator output voltage is approximately equal to the pump-motor input voltage.
- c) The amount of control and electronic load is the same for all missions. All remaining power is distributed to electric thrusters, pumping loads, and high voltage transmitters according to the same ratios as defined for the one-megawatt system.
- d) Full-load power conditioning efficiencies were assumed to be as follows:

1)	Transformers	98%
2)	Frequency Changers	93%
3)	High-voltage rectifiers	99%
4)	Low-voltage rectifiers	95%

Using the systems shown in the figures 2.2-5 thru 2.2-8, the losses for both the power conditioning and the major loads were compiled and presented in table 2.2-13. The resulting radiator areas are shown in table 2.2-15. It is interesting to note the size of the radiators that might be required to dissipate the losses associated with electrical loads utilizing high temperature coolants. At 500°F, they are larger and heavier than the primary radiators of table 2.2-7. Because these radiators are being utilized in the same plants as the main radiators, they should be armored to the same extent as the large ones which will give these radiators a high reliability. The structural weight associated with them will be the same as for the

Table 2.2-13. Summary of Estimated Electrical System Losses (kW)

Components	System Electrical Rating			
	0.35 MWe	1.0 MWe	5.0 MWe	10.0 MWe
Generator losses - 500° to 1100°F coolant	40.0	75.0	275	570
AC Motor losses - 500° to 1100°F coolant	5.5	27.8	163	334
High temperature power conditioning - 500° to 1100°F coolant	5.9	17.5	88	168
Subtotal - High temperature coolant losses	51.4	120.3	526	1072
Low temperature power conditioning - 150° to 200°F coolant	8.9	22.4	129	246
Total - Electrical System losses	60.3	142.7	655	1318
High temperature electrical loads utilizing 500° to 800°F coolants	228	658	3139	6245

Table 2.2-14. Summary of Estimated Electrical System Coolant
Flows (lb/sec)

Components	System Electrical Rating			
	0.35 MWe	1.0 MWe	5.0 MWe	10.0 MWe
Generator coolant - 500° to 1100°F, maximum allowable $\Delta T = 100^\circ\text{F}$	2.1	3.9	14.2	29.4
AC Motor coolant - 500° to 1100°F, maximum allowable $\Delta T = 100^\circ\text{F}$	1.0	1.4	8.4	17.3
High temperature power conditioning coolant - 500° to 1100°F, maximum allowable $\Delta T = 100^\circ\text{F}$	1.0	1.0	4.6	8.7
Subtotal - High temperature coolant	4.1	6.3	27.2	55.4
Low temperature power conditioning coolant - 150° to 200°F, maximum allowable $\Delta T = 50^\circ\text{F}$	1.0	1.0	5.3	10.1
High temperature electrical loads utilizing 500° to 800°F coolants, maximum allowable $\Delta T = 100^\circ\text{F}$ (assumed)	11.8	34.0	162.0	322.0

Figure 2.2-5. Estimated Power Distribution for 0.35 MWe System (Unmanned)

Figure 2.2-6. Estimated Power Distribution for 1.0 MWe System (Unmanned)

Figure 2.2-7. Estimated Power Distribution for 5.0 MWe System (Unmanned)

Figure 2.2-8. Estimated Power Distribution for 10.0 MWe System (Unmanned)

Table 2.2-15. Auxiliary and Secondary Radiator Areas (ft²)

High Temperature Coolant for Power System Components				
Coolant Temperature \ MWe	0.35	1.0	5.0	10.0
1100°F	24	57	247	504
800°F	57	132	579	1180
500°F	167	391	1710	3480
Specific Weight (lbs/ft ²)	(3.63)			
Low Temperature Coolant for Power Conditioning Components and Electrical Loads				
175°F	151	381	2195	4190
Specific Weight (lbs/ft ²)	(2.5)	(2.5)	(3.0)	(3.0)
High Temperature Coolant for Electrical Loads				
800°F	250	723	3450	6860
500°F	740	2135	10200	20270
Specific Weight (lbs/ft ²)	(3.63)			

primary radiators. For these reasons, the same specific weight as for the primary radiator was assumed, 3.63 lbs/sq. ft for the high temperature coolants. For the low temperature coolants, the materials may be aluminum, organic fluids, etc., so the specific weights are less. Those calculated are shown in table 2.2-15.

The calculated pumping power required to pump the coolants are tabulated in table 2.2-16. The powers shown for the "High Temperature Coolant for Power System Components" were added to the primary pump powers of table 2.2-8 to get the total pump power associated with the primary radiator pump weights of table 2.2-1. The pump powers shown for the high temperature loads were not factored into the weights in table 2.2-1 since these

are not considered part of the power conversion system; they were included in table 2.2-16 for the sake of interest and completeness.

2.2-6 Effective System Weights of Electrical Components

In order to optimize the various electrical components relative to the combined electrical weight and efficiency that gives the lowest system weight, an effective system weight is defined using the previously determined system weights of table 2.2-1. For the design study, effective system weights are defined for the generator, the motors, the high-temperature power conditioning and the low-temperature power conditioning.

A. Effective Generator System Weight, W_{ge}

$$W_{ge} = W_g + S_g (kW_g) + S_L (kW_{gL})$$

W_g = actual generator weight = electrical weight plus structure weight

S_g = specific weight of system supplying shaft power

Table 2.2-16. Auxiliary and Secondary Coolant Pump Powers (hp)

High Temperature Coolant for Power System Components				
Coolant MWe Temperature	0.35	1.0	5.0	10.0
1100°F	0.33	0.56	2.25	4.25
800°F	0.56	0.94	3.70	7.00
500°F	1.26	1.82	7.56	13.94
Low Temperature Coolant for Power Conditioning Components and Electrical Loads				
175°F	0.98	1.57	7.2	12.4
High Temperature Coolant for Electrical Loads				
800°F	1.69	4.66	18.36	36.0
500°F	3.48	3.48	34.80	67.6

$$\begin{aligned} \text{kW}_g &= \text{input power to generator} = (\text{kVA} \times \text{p.f.}) \\ &+ \text{kW}_{gL} = \text{kW}_e + \text{kW}_{gL} \end{aligned}$$

kW_e = electrical rating

kW_{gL} = generator losses

S_L = specific weight of system rejecting high temperature electrical losses.

The efficiency of the generator is defined as,

$$\text{EFF}_g = \frac{\text{kW}_e}{\text{kW}_e + \text{kW}_{gL}} \text{ or } \text{kW}_{gL} = \frac{\text{kW}_e}{\text{EFF}_g} - \text{kW}_e$$

Combining this last equation with the first gives the following simplified form for the effective weight.

$$W_{ge} = W_g + \text{kW}_e \left(\frac{S_g + S_L}{\text{EFF}_g} - S_L \right)$$

The effective system weight penalty is simply

$$\text{Gen. wt. penalty} = (S_g + S_L) \text{kW}_{gL} = (S_g + S_L) (\text{kW}_e) \left(\frac{1}{\text{EFF}_g} - 1 \right)$$

B. Effective Motor System Weights, W_{me}

$$W_{me} = W_{pm} + S_e' \text{kW}_p + S_L \text{kW}_{pL}$$

W_{pm} = total pump and motor weight

$S_e' = S_e - S_p$ = specific weight of system supplying electrical power exclusive of the pump weights

S_e = Specific weight of system supplying electrical power to motor

S_p = specific weight of pumps/motors in the system

$$= (W_{BFp} + W_{pRp} + W_{RCP}) \div \text{kW}_e$$

W_{BFp} = weight boiler feed pump/motor

W_{RCp} = weight reactor coolant pump/motor

kW_p = pump motor input power = $kW_{rating} + kW_{pL}$

$$= \frac{\text{hydraulic power}}{(\text{pump eff.})(\text{motor eff.})} = \frac{kW_{HYD}}{(EFF_p)(EFF_m)}$$

kW_{pL} = pump motor electrical losses

Now,

EFF_m = motor efficiency = $\frac{\text{motor output}}{\text{motor input}}$

$$= \frac{\text{motor output}}{\text{motor output} + kW_{pL}}$$

or

$$kW_{pL} = \frac{kW_{HYD}}{EFF_p} \left(\frac{1}{EFF_m} - 1 \right) = kW_{rating} \left(\frac{1}{EFF_m} - 1 \right)$$

Thus, the motor effective system weight is related to,

$$\begin{aligned} W_{me-W_{pm}} &= \frac{kW_{HYD}}{EFF_p} \left[\frac{S_e + S_L}{EFF_m} - S_L \right] \\ &= kW_{rating} \frac{S_e + S_L}{EFF_m} - S_L \end{aligned}$$

The effective system weight penalty is simply,

$$\text{Motor wt. penalty} = (S_e + S_L)(kW_{rating}) \left(\frac{1}{EFF_m} - 1 \right)$$

The auxiliary coolant pump weight has been assumed to be included into the primary radiator pump weight. This approximation allows for analytical simplification and should be of little consequence to the results. The hydraulic losses in the boiler feed pump and reactor-coolant pump are assumed to go into the fluid being pumped as useful heat.

C. Effective High-Temperature Power Conditioning System Weight, W_{hpe}

$$W_{hpe} = W_{hp} + (S_e + S_L) kW_{hpL}$$

W_{hp} = weight of "hot" power conditioning component

kW_{hpL} = electrical losses in "hot" power conditioning component

In terms of the component efficiency and rating, where

$$\begin{aligned} EFF_{hp} &= \frac{kW_{rating}}{kW_{rating} + kW_{hpL}} \text{ or } kW_{hpL} \\ &= kW_{rating} \left(\frac{1}{EFF_{hp}} - 1 \right), \end{aligned}$$

the system weight penalty is,

$$W_{hpe} - W_{hp} = (S_e + S_L) (kW_{rating}) \left(\frac{1}{EFF_{hp}} - 1 \right)$$

D. Effective Low-Temperature Power Conditioning System Weight, W_{Lpe}

$$W_{Lpe} = W_{Lp} + (S_e + S_L) kW_{LpL}$$

W_{Lp} = weight of low temperature power conditioning component.

S_L = specific weight of system rejecting low temperature electrical losses.

kW_{LpL} = Electrical losses in low temperature power conditioning component.

Likewise, the system weight penalty is,

$$W_{Lpe} - W_{Lp} = (S_e + S_L) (kW_{rating}) \left(\frac{1}{EFF_{Lp}} - 1 \right)$$

2.2.7 Generator Rotor Inertia and Transient Response

Another important part of this task was to examine pertinent relationships between the various electrical components and the whole power system. One of the relationships examined warrants special discussion; it is the relationship between generator rotor inertia and rotor overspeed response time.

It has long been recognized that there is a considerable amount of inertia and energy associated with the high-speed inductor alternator. The following approximate analysis indicates of magnitude of this energy and its effect on speed transients of the turbo-alternator.

As a basis for estimating the minimum time to reach five percent overspeed*, the following conditions or assumptions were specified.

- a) The generator is assumed to be under full electrical load at design speed at time-zero (t_0).
- b) At time-zero, all the electrical load is tripped off the generator and all the turbine shaft power supplying this load goes into accelerating the rotor.
- c) During the period of acceleration, the turbine drive torque is constant as well as the drag torque due to friction. Thus, the accelerating torque (L) is constant.

Now, by definition,

$$L \equiv I\alpha \equiv I \, dw/dt \quad (\text{in-lb})$$

or

$$(t-t_0) = \Delta t = I/L (w-w_0) = \frac{I w_0}{L} (\Omega) \quad (\text{seconds})$$

where

$$\Omega = \frac{\% \text{ overspeed}}{100}$$

w_0 = shaft speed, radians per second.

also, by definition,

$$L = \frac{(\text{HP}) (396,000)}{2\pi N_0} = \frac{(\text{kW}_e) (6600)}{(0.746) w_0} \quad (\text{in-lb})$$

*The generator rotor is usually designed for at least five percent overspeed stress.

thus,

$$\begin{aligned}\Delta t &= \frac{I_{wo}^2 (\Omega) (0.746)}{kW_e (6600)} \\ &= \frac{I_g^{wo2}}{2} \left(\frac{B_I (0.746)}{kW (3300)} \right) \quad \text{(seconds)}\end{aligned}$$

where

$$B_I = 1 + \frac{I_t}{I_g}$$

I_t = inertia of turbine, in-lb sec²

I_g = inertia of generator, in-lb sec²

A check of the most promising generator designs in the generator parametric screening study revealed that there is a definite pattern to the relationship between the generator electrical rating and the kinetic energy in the rotor; the relationship is shown on figure 2.2-9. Note that the energy varies from approximately 100-million inch pounds to 10,000-million inch pounds. Eight parametric designs were used at each rating so the data shown is for 32 different designs and may therefore be considered representative.

With this relationship between electrical rating and kinetic energy, the transient times to reach five percent overspeed were determined and are shown on figure 2.2-10. A constant ratio of turbine inertia to alternator inertia was assumed that was larger than any resulting from those turbine inertias of table 2.2-6 and corresponding alternator inertias. As a result, all possible turbo-alternator designs should fall between the two lines of figure 2.2-10.

The resulting times are considerable and should allow ample freedom in designing the speed control or trim system to prevent transients from exceeding five percent. At ten percent overspeed, the times would be double those shown.

2.2.8 Electrical Component Design Concepts Related to System Operation

The Rankine configuration being considered is the basic three-loop configuration shown conceptually on figure 2.2-10. The components of interest in the configuration can be classified as the "high temperature" and "low temperature" components.

Figure 2.2-9. Rotor Kinetic Energy Versus Generation Electrical Rating

Figure 2.2-10. Time Required to Accelerate Rotor to 5 Percent Overspeed Versus Generator Electrical Rating

For purposes of the following discussions, the high temperature powerplant components consist of

- a) turbogenerator and accompanying boiler, condenser and heat exchangers,
- b) transformers, and
- c) pumps/motors.

The low temperature components include the power conditioning components such as

- a) frequency changers,
- b) rectifiers,
- c) contactors, and
- d) control circuits such as generator field control.

The transmission lines are classified as both high and low temperature depending on their location.

Although only one three-loop system is shown, there will probably be a number of loop systems to provide reliability thru redundancy. Also, even if a single-loop system would prove to be reliable, at least two counter-rotating turbo-alternators may have to be used to minimize the amount of station keeping torque required to stabilize the space craft during orbital startup; by the time operating speed is reached, the generator rotor alone will have over 10 million* ft lbs of energy stored in it. Because of this, shutdown of one generator may require the other to be shut down also to balance the decay torque.

A. High Temperature Components

1. Physical Configuration. The high temperature power conversion equipment (boiler, turbo-alternator, pumps, etc.) that utilize liquid metal coolants will probably all be located in a hermetically sealed, insulated can (module) close-in behind the reactor/shield in the rocket payload nose cone. The power conditioning equipment utilizing the same coolants will also be located in the module. Access will be gained by hermetic feed-throughs for the electrical connections, the (lithium) reactor connections and the (NaK) radiator connections. The potassium loop for the turbo-alternator will probably be a closed loop within the module unless the NaK radiator loop is eliminated in favor of a direct potassium (condensing) radiator. Although the latter can save significant system weight, the likelihood of its use is less because of the more complicated development and operating problems.

The equipment in the module, especially the turbo-generator and motors, will be suspended in the module and the module

*For a typical 0.4 MVA rating; at 12 MVA, the energy increases to about 800 million ft lbs.

will in turn be suspended in the rocket/radiator structure. The suspending members will be designed to provide maximum restraint to sustain launch loads and maximum flexibility to accommodate thermal expansion. A safe assumption would then be that center of gravity mounting points (2 or 3) will be utilized and that pipe loads of several hundred pounds (say 100 to 500 for small to large pipes respectively) may be imposed on the cooling fluid inlets and outlets that connect directly to the coolant loops.

The module will be hermetically sealed for utilizing a hot cover gas to provide uniform preheat and soaking during prelaunch, launch and orbital startup. The same module then provides meteoroid protection during flight. To avoid the possibility of an unexpected transient occurring by loss of cover gas through a meteoroid puncture, the gas will probably be released shortly after startup and the interior of the module exposed to space vacuum. There is every likelihood then, that there will not be a cover gas makeup system associated with the module and that the use of cover gas in a generator stator or motor stator will not be practical from the system standpoint. The modular approach will permit some control of the incident heat flux on the electrical components but in a worse case design, it would be assumed that the component is surrounded by an infinite heat sink at the primary radiator temperature.

2. Prelaunch Ground Operations and Orbital Startup. The power conversion system will undoubtedly undergo checkout operation on the ground prior to installation (possibly up to several thousand hours). The heat will be supplied to the boiler from an artificial source which will probably use lithium as the heat carrying fluid. The waste heat will be rejected from the condenser by an artificial (Nak) source. The potassium in the boiler-turbogenerator-condenser loop will probably be the same potassium that will be used later in flight. In other words, the potassium loops will probably be sealed during the ground checking and fluid conditioning phase, and they will remain sealed thereafter; this is in contrast to the reactor side of the boiler and radiator side of the condenser which will have to be reopened to the atmosphere and will require cleaning prior to integration with the rocket on the launch pad.

On the launch pad, the reactor and radiator loops will be refilled with alkali metal which will then be circulated by the pumps through the systems (where required) to help maintain preheat during pre-launch and post-launch orbital startup. During launch, (or landing) all rotating machinery will probably be shut down.

The preheat cycle on the power conversion equipment will probably be a uniform soak of 800° to 1000°F on the ground and a gradual decrease in temperature to about 400°F just prior to startup. From there, the equipment will undergo transients to the operating temperatures.

The startup of the turbogenerator will probably occur 4 to 10 hours after orbit is achieved and will probably be a "dry" start; that is, there will not be a flooded generator rotor cavity except for the minimal shaft-seal leakage that occurs as the potassium is simultaneously released to the turbine (vapor) and turbogenerator bearings. Also, the startup will probably be carried out via at least one and possibly two part-speed/hold conditions to check performance before going on to higher speeds. At one of these holds, the power will be transferred from the auxiliary power startup unit to the turbogenerator. Whether special controls will be included to control the bearing-cavity/shaft-seal pressures during startup remains to be seen; some control will occur automatically via the fact that the supply pump will also be running at part speed. For purposes of conceptual designs, most designers assume (1) that one hold would occur at 1/2 rated speed, (2) power is transferred there and (3) pump speed varies according to resulting generator frequency once power transfer is achieved.

The pump motors will undoubtedly be started with flooded rotor cavities since the pump loops are flooded and contain single phase fluids.

The details of whatever startup sequence is finally utilized will probably not allow for the presence or sequence of events that once started are not irrevocable by ground signal. Although the startup sequence will probably be carried out automatically by a space controller, the ground controller will want the capability of interrupting the sequence at any time, controlling it from the ground and likewise returning it to the space controller at any time. Since no single accepted startup sequence has ever been decided upon, the designs of the present design study reports do not take startup requirements into detail consideration.

More recent information indicates that it may be desirable for the rotor cavity to be vented to space (to obviate bore-seal requirements) and that zero-leakage (less than 10 lb/yr) shaft seals be used to seal the bearing cavities. It is not specifically known how this might effect startup/shutdown requirements.

3. Mission Shutdown and Restart. On long endurance missions such as those which will utilize Rankine systems, at least one and most likely several shutdown/coast periods can be expected. These may amount to over half the mission time and will probably not be complete shutdowns but will be part-speed and/or power shutdowns (say to 2/3 or 1/2 speed) to maximize the operating life of the rotating components. Thus, any designs which prevent shutdown and restart in space must be avoided. Beyond that, little more can be said about the details of the shutdown until the missions are defined. This lack of definitive startup/shutdown sequences is to the component designers present advantage since he can specify what is needed.

During the coast periods, the load on the generator may go as low as 10 to 15 percent of the rated power. Although parasitic load will be available for absorbing excess power (up to 100 percent of rated power) during transients, it should not be expected that such a power dump would be utilized in an extended coast period. The minimum power output is limited by the minimum allowable auxiliary radiator temperature (approximately 300°F) which may require up to 15 percent of rated output being dumped into a parasitic load at zero net power output.

4. General Space Environment Considerations. Unless the components are in intimate contact with the alkali metals in the power loops or, as mentioned above, unless the necessity for avoiding such is proven, one can assume that the components will be subject to the vacuum of space.

Other rigors of space such as meteoroid bombardment or solar heat flux need not necessarily be considered as design criteria since the electrical equipment will probably be well protected by the modules, radiators, etc., which surround them. The one exception to this statement is the transmission lines which will probably be openly exposed to space.

So far as radiation goes, the most severe dosage will come from the reactor; solar radiation will not be a limiting criteria (except possibly in a solar probe mission which is not specifically required for the design studies).

Some system derating can be expected to occur indirectly due to meteoroid damage and subsequent derating of the radiators. An enlightening example of the effects such deratings have on the system operating point is shown in Section C below.

Turbogenerator speed control will be accomplished by electrical frequency and voltage sensing. Speed correction will be indirectly handled by varying the amount of electrical load being dumped into a parasitic load so that the turbogenerator output (and thereby the speed) remains constant regardless of the system load transients. For large system load transients or internal generator shorts and opens, reactor control and/or condenser pressure control may also be utilized. Conjectured overspeed due to loss of turbine load: not more than 5 percent rated speed; conjectured underspeed due to control tolerances: not more than 5 percent and most likely of the order of 1 percent.

B. Low Temperature Components. Very little background information relative to the system design aspects of low temperature components could be found in the system reports reviewed. Thus, probable trends could not be identified as easily as they were on the high temperature components. The following generalizations can be made however.

The low temperature components will be remotely isolated from all high temperature components including the radiators making up the structure supporting the power conversion units. They will probably be located in the actual payload section where

a "habitable" environment exists. The high and low temperature components will be interconnected by transmission lines. Cooling will be accomplished by cold-plate-cooling to space or by using intermediate cooling fluids such as water or organic fluid.

Notice that, because of this physical and thermal isolation between high and low temperature components, "cumbersome" design situations arise. Three examples are:

1. If large blocks of low voltage, dc power are required, a transformer must first step the generated voltage up for distribution down the interconnecting transmission line, a second transformer would then step it back down before entering the rectifier. The first transformer would be cooled with a high-temperature coolant while the second might have to be cooled by a low-temperature fluid to avoid imposing penalizing heat loads on the low temperature rectifier units. Using high-temperature coolant on the payload end would require coolant lines in the interconnecting boom, an unlikely situation.
2. The motor power (generated voltage) must be stepped up, transmitted to the cold frequency changer, transmitted back to a second transformer which then steps the power back down to the hot motor.
3. The generator output must be "sensed" at the cold end of the payload (which is remote from the generator output) and the generator control and field power must be transmitted over lines exposed to space hazards (thereby requiring very reliable protection).

All of these design situations impose weight penalties on the system through redundant transformers, excessive transmission lines, extra meteoroid protection, etc. By eliminating the "cold" power conditioning components and replacing them with units capable of operating at 500°F or above, not only is the above weight penalty eliminated but the total radiator weights drop also. To illustrate the latter point, the "cold" power conditioning cooling load on a 1 MWe system will be approximately 22 kW compared to the 120 kW cooling load for the "hot" (500°F) equipment (table 2.2-13); however, the weight of radiator to dissipate this "cold" loss is 950 pounds compared to 1420 pounds for the "hot" radiator. In other words, 40 percent of the total cooling radiator weight is required to dissipate 15 percent of the total cooling load. Had a single 500°F coolant been used, the weight of the "hot" radiator would increase by 260

pounds, the cold radiator weight would decrease by 950 pounds, a net reduction 690 pounds, and the final weight is only 71 percent of the original two radiator weights,

Weight savings like the above, not to mention elimination of extra pumps, transmission lines, transformers, etc., are justification for pursuing high-temperature power-conditioning technology.

C. Electrical Component Coolants. According to the system reports reviewed, coolant inlet pressures will probably be of the order of 50 to 100 psia and throttling may be employed to decrease it to fit the specific need in the components. The outlet back-pressures are either of the order of 25 psia or for the alternator, may be near the vapor pressure in the turbine condenser (potassium coolant only). Unless specifically requested to do so, nuclear system designers will not use the condenser as a coolant dump tank; thus, unless required otherwise, one may assume the outlet back pressure is about 25 psia.

The flow through the various electrical components may be either by individual parallel paths or by a single path through a series of components; a definite preference between the two could not be identified in the system reports reviewed. For purposes of the design study, it was assumed that each component has its own, individual flow.

A part of the study was directed toward defining the probable effects of off-design operation relative to coolant temperatures, coolant pressures and coolant flows. The off-design situations shown on figure 2.2-11 are based on the literature reviewed and cover (1) 50 percent and zero percent net power output to load, (2) half-speed, zero net power operations, and (3) 25 percent and 50 percent reductions in high temperature coolant radiator area. Naturally, there are mute changes occurring in several other design parameters; however, for purposes of illustrating order of magnitude (conceptual) changes, it is necessary to consider only those shown.

The 25 percent reduction in radiator area is typical of the loss of radiator area over the mission life; the 50 percent reduction in area might represent an emergency abort condition where the original mission objectives are cancelled and survival until return to earth becomes the mission objective.

The zero percent net power output and/or half-speed state would be at startup or during a coasting period; the 50 percent net power is a typical operating condition.

A major design consideration that can be drawn from figure 2.2-11 is that the electrical components may have to operate reliably at as much as 100°F over design coolant temperature if an aborted mission is to not end disastrously.

Figure 2.2-11. Off-Design, Coolant Flow Changes

2.3 SYSTEM DEFINITION FOR CONCEPTUAL DESIGNS

The primary purpose of the system definition task was to define system parameters such that, for each of the various conceptual designs of the systems components may be made. The system definition is based on the Power Use and Mission analysis of section 2.1. With these data, the relative magnitudes of ac and dc power, voltage levels, generator frequency, bus configurations, and excitation system are defined. In addition, a brief discussion on the control and protection aspects of power systems is presented to form a basis for future mission studies.

2.3.1 Electrical System Definition

Figures 2.3-1 through 2.3-5 show a general system configuration for each of four conceptual missions. These missions are based on data generated in the mission studies. Note that these systems consist basically of one each of the required power generation and power conditioning equipment. Generally this approach will lead to lighter total system weight. For long duration missions requiring multi-megawatt electrical power systems, a configuration of several lower rated systems, with a total capacity equaling or exceeding the required mission power is suggested. The primary reason for this is to add further assurance of mission success. Space vehicles depending on a single power system would be lost if some vital part should fail. However, with a vehicle relying on multiple power generating systems, a single failure would not result in complete loss of functions. Hence, mission reliability can be increased by furnishing a power system with several power sources.

A second advantage of this approach is the ability to construct larger rated systems by simply adding additional power modules to a basic electric power system.

From figures 2.3-1 through 2.3-4, it is noted that the primary loads (propulsion) are generally made up of many sub-modules. Hence, it would be unrealistic to make a single power conditioning system for all the engines. The better approach is to modularize the engines and furnish each with a separate power conditioning system including the necessary control and protection to assure proper engine start and survival during an engine fault. This approach is somewhat impractical from the standpoint that the single engine rating will be at most several kilowatts while the mission will require one or more megawatts of engine capacity. This situation means that the number of power conditioning equipment may be quite large. A compromise can be made which will assure balanced thrust vectors, minimize power transients, and still provide improved engines system reliability. This is accomplished by choosing a number of engines which will supply a balanced thrust vector and connecting them to a single set

Figure 2.3-1. Preliminary Concept No. 1 for Unmanned Scientific Probe

Figure 2.3-2. Preliminary Concept No. 1 for Manned Interplanetary Mission

Example Shown: $x = 25\text{KW}$

Figure 2.3-3. Electric Engine Module (1 of n)

Figure 2.3-4. Preliminary Concept No. 1 Synchronous Orbit Space Station

Figure 2.3-5. Concept for Lunar Base

of power conditioning equipments as shown in figure 2.3-1. For the ion-propulsion engines, as many as seven different power conditioning units per engine are required. During certain engine fault conditions, three of these supplies must be shut down while the others should remain operating. The arrangement for this concept is shown in figure 2.3-3. The bulk of the required engine power is conditioned in the three rectifier units. Note that one rectifier does not require a transformer. This is accomplished by rating the generator at an alternating voltage to achieve the required 3000 volts, d-c power. Since this portion of the engine represents 75 percent of operating load, a weight savings results. Because the rectifier bridge is a three-phase full-wave type, the neutral of the generator cannot be connected to the common electrical connection of the engine. This does not result in a serious drawback, however, because the rectifier forms a balanced three-phase load and all other system loads will be isolated by a transformer. Hence, all system loads and the engine can have a common connection.

If the basic single electric-power system concept is abandoned in favor of a multiple electric-power system concept, many other configurations can be realized. These configurations fall into one of the three following categories as depicted in figure 2.3-6

- a) electrically isolated system load buses,
- b) electrically isolated system load buses with provisions for interconnections, and
- c) paralleled system load buses.

Parallel systems are potentially more flexible because no restriction on the rating of a load is imposed. Other benefits are the increased fault clearing capacity and non-interruption of power as generation systems or loads are added to or removed from the parallel system. The most severe drawback to this approach, however, is the desire not to have a throttle control for the turbine; turbine speed must be controlled in order to properly share load among the paralleled generators. Other considerations, though not serious, are the more complex control and protection circuits for locating system faults and forcing real and reactive load sharing. In some types of electric-power systems, adequate parallel operation can be achieved by depending on prime movers with a soft-speed-load characteristic. With systems of this type, full utilization of power potential cannot be realized and further the system frequency will vary several percent as the system loads vary. The system utilizing this particular approach to parallel operation would have to be matched to this requirement.

Another approach to parallel system operation is to utilize the fact that the bulk of systems loads are direct-current loads. Since parallel dc systems do not require speed control to assure load sharing, the need for turbine

Figure 2.3-6. Multiple Power Source Concept of Achieving a Specific Rating

speed control is removed and artificial droop can be designed into the generator excitation system to assure proper load sharing.

The approach would be to parallel the ac generators through the rectifiers, sense load current differences, and alter load division by controlling generator excitation with voltage magnitude and load current difference feedback signals. For this approach to work, the dc load used in the paralleling feedback loop should be in the range of 75 to 80 percent of the total load on each generator. Further, a protective function should be included to assure that the paralleled dc load and the sum of other generator loads do not exceed the capacity of any generator system. If the generating systems are quite stiff, the actual

voltage change will be quite small thus not affecting the non-paralleled loads. A further advantage of this approach is that speed control can be accomplished by means of parasitic-load speed controllers.

Because the bulk of the loads must be direct current and must represent the major portion of system capacity, this approach will not be applicable to all missions. For example, the major dc loads in figures 2.3-1 and 2.3-2 are electric engines, but the loads are split into several smaller rated power conditioning equipments to prevent large load changes resulting from such fault conditions as engine arc over. Obviously, if each group of electric engines is to operate isolated from other engines, the engine group cannot have paralleled power conditioning equipments. The systems defined for this mission would therefore be more adaptable to an isolated arrangement of power systems unless the power systems are paralleled at the load side of the control contactor (ac side of all loads).

From the above and analysis of various types of loads for the conceptual missions, the isolated type of multiple power-generating systems appears to be the best choice. To enhance reliability, a means of multiple load connection points and provisions for connecting a single load to more than one power source is desirable. The system configurations for multiple power systems therefore falls within category two of the various types of system interconnections.

Isolated power generation systems with load-bus interconnection provisions will be the selected approach for all systems whose individual loads are less than 50 percent of a single generator rating. The various loads will be connected to load buses arranged to balance the load among the several generators. Figure 2.3-7 shows an arrangement of multiple generation systems for isolated operation. For loads requiring more than the capacity of a single generator, parallel operation must be considered. While this situation is not foreseen for these conceptual missions, future requirements, such as advanced Lunar Bases where multi-megawatts may be required may justify the benefits of paralleled power systems and may warrant the increased complexity in the turbine speed control loop. The power network would be similar to present day terrestrial systems.

Because the various conceptual missions cover a wide range of power ratings, the approach to conceptual equipment design will be to choose various equipments from figures 2.3-1, -2, -4, and -5. A larger-rated system can be established by taking a multiplicity of the smaller-rated systems.

2.3.2 System Control and Protection

A. Generator Excitation Control. The function of the excitation circuits of a synchronous generator is to provide magnetic flux in the air gap of the generator such that an

alternating voltage is provided to system loads. The excitation circuit may be either open or closed loop, i.e. the terminal voltage of the generator changes with load current due to generator internal impedance or changes very little by altering the magnitude of air gap flux by a feed-back network and reference. The latter type is required for the systems under study. In isolated systems -- only a single generator per system load -- the excitation circuit need only to control generator voltage by altering the magnitude of current in the generator field. There are several ways of achieving this, however, the most practical scheme seems to be to utilize a portion of the generator power such that no auxiliary sources of power need be developed. Because the inductor-type generator is apparently the best choice of the several types to meet these applications, the excitation power is quite large (see generator parametric data, section 3.1). Further, the possibility of faults in the power distribution system requires that the excitation circuit have alternate sources of power. Others⁽⁹⁾ have analyzed several approaches and have shown that a combination of current and voltage feed-back provides a good method of meeting the several requirements of generator excitation, see figure 2.3-8.

Figure 2.3-7. Single Line Diagram of Isolated-Load Bus System

Figure 2.3-8. Generator Excitation Control Circuits

1. Exciter-Regulator Power Circuit. Since the power circuit represents the highest percentage of exciter-regulator weight and losses, the parametric data generated for transformers, rectifiers, and frequency changers is directly applicable. The parametric data for current transformer required excitation power under fault conditions may be estimated from the voltage transformer parametric data. Rating of the current transformer is determined from the excitation current required to furnish the desired percentage of short circuit current, the field resistance, rectifier conversion factor, and efficiency. Three-phase transformer rating equals

$$I_e^2 R_f / (\eta \times \text{F.F.})$$

where,

I_e = excitation current (dc) at short circuit

R_f = Field resistance

η = Circuit Efficiency

F.F. = Form factor

= 0.955 for 3-phase, full-wave bridge

= 0.825 for 3-phase, half-wave bridge.

These data may be obtained from the generator parametric data. For example:

1.2 MVA, 1600-Hz, 800°F coolant generator

1 p.u. field current = 40 amps at rated voltage

R_f = 4.18 ohms @ 1200°F

Need 3 p.u. short circuit current

I_e = 133 amperes for 3-phase short circuit

Current transformer rating

$$(133)^2 \times (4.18) / \left[(0.825)(0.95) \right] = 94.5 \text{ kVA, maximum field power.}$$

Use one 96 kVA, 3-phase, 1600-Hz, 800°F coolant or 3, 32 kVA, single-phase, 1600-Hz, 800°F coolant transformer(s).

2. Exciter-Regulator Control Circuit. The circuits required for controlling the firing angle of the controlled rectifiers will be about ten percent of the exciter-regulator weight. The control circuits function to cause the terminal voltage of the generator to remain at nearly a constant voltage regardless of loading. The terminal voltage is sensed, compared with a reference. The error signal developed determines the firing angle of the controlled rectifier. When the generator is operating in a parallel system, the exciter-regulator performs a second function: the distribution of reactive power among the several generators. This is accomplished by detecting the portion of reactive current in the generator current and comparing this with the reactive current contribution of the paralleled generators. If the currents are not balanced, the reactive-current error signal is added to the terminal-voltage error signal to either reduce the field current (in the case of too large a reactive current) or increase the field current (in the case of too small a reactive current). Reactive current is sensed by means of a current transformer loop (low-level signal) in conjunction with the sensed voltage. There are many acceptable schemes available for accomplishing this.

B. System Protection. The function of the protection circuits is to increase system reliability by monitoring system conditions, determining when and what portion of the system is operating abnormally. The action then is to isolate the faulted portion, thereby returning the rest of the system to normal operation. Of course, if all portions of the system must operate in order to meet system objectives, no advantage can be gained by the addition of protective functions. Hence, the electric-power system must either have reserve power or alternate modes of operation to fully utilize the protective circuits. Circuits commonly used are:

- 1) over and under voltage: monitor excitation system
- 2) overcurrent: monitor distribution system to locate faulty transmission lines and load busses and utilization equipment,
- 3) over and under frequency: monitor turbine speed control system, and
- 4) in parallel systems, real and reactive load division: monitor exciter and load-division circuits.

The circuits for determining these functions are generally available since similar circuits have been developed for many other aerospace applications. The signals developed by these circuits are processed through combinational and sequential logic circuits to determine the location of the fault. The action will generally be to operate a circuit breaker or to deactivate one of the system components.

The logic circuits can be programmed to make decisions as to the connection of various loads in order of their importance.

This is done by monitoring system capacity and either adding the highest priority loads to or removing the lowest priority loads from their respective load busses. In addition, critical loads can be arranged to have alternate load busses in the event of permanent fault on one load bus. The sensing, logic, and amplification circuits will generally assume about one percent of total system weight depending on the degree of sophistication. These circuits will consist of integrated circuit elements for low-level sensing and logic functions and discrete resistors, diodes, and transistors or thyristors for high-level sensing and power amplification.

3. CONCEPTUAL DESIGNS

The purpose of the conceptual design task is to illustrate a number of representative designs of the components studied and present them in more detail than in the generalized parametric screening design studies. The designs presented must therefore be representative of the designs determined to be the "best" or most representative in the screening studies. Likewise, since the conceptual designs will be of interest to system layouts, the designs must be representative of those of the typical mission systems, section 2.3. The conceptual designs shown within are based on considerations relative to both requirements. The specific reasons behind the selections of the various conceptual designs shown are presented in the individual sections following.

The choice of potassium component coolant temperature is common to all the "high-temperature" conceptual designs and is discussed in the generator conceptual design section below since it is of most significance to the generator.

3.1 GENERATOR CONCEPTUAL DESIGNS

3.1.1 Selection of Representative Ratings

As seen from the previous section on electrical system definitions (section 2.3), the generators typical of the mission electrical systems are:

350 kVA,	480/ 832 V,	0.75 p.f.
1000 kVA,	1300/2260 V,	0.85 p.f.
2100 kVA,	1300/2260 V,	0.9 p.f.
10000 kVA,	1300/2260 V,	0.9 p.f.

The frequencies, speeds, and coolant temperatures are not specified.

The parametric screening studies for the generator (reference 6) resulted in the frequencies, speeds, and voltages, at the ratings and temperatures studied as shown in table 3.1-1. A quick comparison of the two sets of generator rating data leads to the conclusion that there are several similarities except in the voltages and mid-power ranges.

Since the frequency, speed, and coolant temperature were not specified for the mission systems, it was necessary to examine the generator parametric data and system data in detail to select most appropriate values for the conceptual designs. For example, the system specific weights as presented in table 2.2-1 indicate that an 800°F alternator coolant gives a lighter system than the 500° or 1100°F coolant systems. This is because, in going from 500° to 800°F, the generator weight increase is significantly less than the auxiliary radiator weight decrease. In going from 800° to 1100°F, however, the generator rotor material

Table 3.1-1. Summary of Generator Parametric Study,
Selected Speeds, Frequency and
Voltages per Reference 6

Generator Rating* (MVA)	Coolant Temp. (°F)	Selected Frequency (Hz)	Selected Speed (rpm)	Selected Volts (L-N)	Electrical Weight (lbs)	Frequency (%)
0.4	500	2000	40000	500	294	91.3
1.2	500	1600	24000	500	1008	92.3
6.0	500	1200	12000	500	5257	91.9
12.0	500	1200	12000	500	9966	92.5
0.4	800	2000	30000	500	329	91.2
1.2	800	1600	19200	500	1151	92.1
6.0	800	1200	12000	500	5317	92.4
12.0	800	1200	8000	500	16619	93.5
0.4	1100	1500	18000	500	536	91.8
1.2	1100	1200	12000	500	1749	92.4
6.0	1100	800	6857	500	10644	92.8
12.0	1100	800	5333	500	24906	93.2
* All at 0.85 p.f.						

must be changed to one with the required higher temperature properties (see rotor materials selection discussion, reference 6) which results in a finite jump in generator weight. The jump in generator weight is significantly greater than the decrease in auxiliary radiator weight.* Thus, the only way the 1100°F coolant could be lighter and more competitive than an 800°F coolant is to eliminate the auxiliary radiator system and reject the generator losses in the primary (turbine condensate) radiator. Recent studies** indicate the primary radiator temperature will be more like 1300°F instead of 1100°F and if such

*Compare for example, the combined generator and auxiliary radiator weights of table 2.2-1 at the 1-MWe power rating. The combined weights are 2428, 1631, and 1946 lbs. for 500°, 800°, and 1100°F, respectively.

**Contracts NAS 3-10933 and NAS 3-10934.

is the case, the rotor material would again have to be changed to meet the 1300°F coolant temperature requirements. This time however, because of the limitations of magnetic rotor materials available or under development, the increase in generator weight in going from 1100° to 1300°F is orders of magnitude greater than the increase in going from 800° to 1100°F. For example, the 1400°F, 1-MWe (1.2-MVA @ 0.85 p.f.), generator in reference 6 weighs about 6600 pounds (electrical weight alone) compared to the 1100°F, 1-MWe generator weight of 1749 pounds (table 2.2-1); both generators are operating near their maximum allowable speeds also.

Because of the above considerations of minimum system weight for the ratings studied, it was decided to use the 800°F potassium coolant for the conceptual designs; not only for the generators but for the a-c motors and transformers as well. As indicated by the curves of figures 3.1-1 and 3.1-2, the minimum alternator weights do not necessarily occur at 800°F but occur over a range of temperatures. The 800°F coolant temperature was selected from this range because of the extensive previous parametric data at that temperature. Likewise, in accordance with the discussions of reference 6, the weight curves of figures 3.1-1 and 3.1-2 are not continuous as shown between 800° and 1100°F and between 1100° and 1400°F. There is a finite jump at the temperature when the rotor material is changed. The shape of the discontinuous curve is not definitely known.

Since the speeds and frequencies were not specified, it was decided to select values as close as possible to those of table 3.1-1 at 800°F. Likewise, since a 1000 and 2100 kVA generator would be similar, it was decided to eliminate the 2100 kVA design in favor of a design of the order of 6000 kVA so that the four conceptual designs ratings correspond to the four ratings of the parametric designs, that is, 400, 1200, 6000 and 12000 kVA. The final speeds were selected after further consideration relative to (1) pole face losses, (2) rotor stresses and associated safety factor for overspeed, (3) prediction of reliable operating life over 40,000 hours with limited, short-term materials data, and (4) the most likely turbine speeds. The speeds selected, shown on table 3.1-2, are slightly lower than those of table 3.1-1; the relative location of these speeds to the parametric study speeds is shown on figure 3.1-3. Remember that the selected speeds can not vary in a continuous manner but must be according to finite steps corresponding to integral pole pairs and 400 Hz frequency increments.

As also seen in table 3.1-2, the voltages of the conceptual design alternators were selected according to selected values of the parametric screening studies rather than the desired system values in section 2.3. The reason for the lower voltages is added reliability at the expense of additional transformers and system weight.

The establishment of a safe working voltage for inorganically insulated magnet wire requires consideration of several

Figure 3.1-1. Parametric Weight and Efficiency Summary, Alternator

Figure 3.1-2. Specific Weight Parametric Summary, Alternator

Figure 3.1-3. Parametric Speed Summary, Alternator

Table 3.1-2. Conceptual Generator Parameters

Rating: (kVA)	400	1200	6000	12000
Power Factor: (lagging)	0.85	0.85	0.85	0.85
Voltage: (3 Phase)	500/866	500/866	500/866	500/866
Amperes per phase:	267	800	4000	8000
Frequency: (Hz)	2000	1600	1200	1200
Efficiency: (%)	92.5	94.0	93.9	94.2
Speed: (rpm)	24000	19200	9000	7200
Electrical Weight: (lbs)	381	1053	6270	15850
Total Weight: (lbs)	641	1545	9360	22970
Total Weight/Elec. Weight	1.68	1.46	1.49	1.45
Coolant & Lubricant:	Subcooled Liquid Potassium			
Inlet Temperature: (°F)	775	775	775	775
Rotor Coolant Flow:* (lb/sec)	0.21	0.42	1.72	2.29
Stator Coolant Flow:* (lb/sec)	1.42	3.24	11.4	20.5
Bearing & Seal Coolant Flows: (lb/sec)	0.05*	0.11*	5.44**	13.2**
Approx. Potassium Charge (lbs)	11	18	97	210
<p>* For $\Delta T = 50^{\circ}\text{F}$ (approximately)</p> <p>** For $\Delta T = 100^{\circ}\text{F}$ (approximately)</p>				

important factors. These are insulation composition, material form, and operating temperature. As a result of testing and evaluation performed on NASA Contract NAS 3-4162 and NAS 3-6465, magnet wire insulation based upon served glass fiber with additional inorganic fillers and glassy binders has been selected as best for operation at temperatures of 900° to 1300°F. This insulation is known as Anadur and is produced by Anaconda Wire and Cable Company. The fully processed form of this insulation does not achieve complete density, but is porous. Therefore, a designer must consider the insulation to be spacing rather than a complete insulating barrier. In addition, the glass-based binders in this composition begin to lose electrical insulation resistance at 1000°F and are very poor insulators above 1300°F. Thus, the electrical stresses of voltage potential must be kept low if high current losses are to be avoided.

The total electric strength of Anadur, 0.0068-inch thick, at 1100°F is approximately 400 volts. This results in a typical value of about 60 volts per mil. This form of insulation must be used with supplemental ground and phase insulation composed of high purity aluminum oxide. For efficient utilization of available conductor and insulation volume in a magnetic core, the ceramic supplemental insulation must be of minimum thickness consistent with production and winding capabilities. The electric strength of aluminum oxide at 1100°F varies from about 60 to 150 volts per mil. For high purity alumina about 0.018-inch thick, typical electric strength is about 100 volts per mil at 1100°F.

Thus, in order to assure a margin of safety to compensate for insulation defects created in processing, mechanical shock in operation, and transient overvoltages, an operating limit of 500 volts at temperature should be maintained. Electrical current leakage with accompanying excessive losses will result if higher operational voltages are attempted.

3.1.2 Generator Descriptions

The basic design and performance parameters of the conceptual design generators are tabulated in table 3.1-2. The voltage, speed, coolant temperature, and frequency were determined as part of the contract; the power factor was specified. The total weight of the generators is greater than the electrical weight since the workhorse parts not directly associated with electrical design (e.g., bearings, stub shafts, coolant, end bells, mounting flanges) are included in these figures. Likewise, the weights and efficiencies of table 3.1-2 do not necessarily correspond to those of table 3.1-1 since the conceptual designs represent improved designs over those of the screening studies.

A. Construction Summary. The significant features of the generator electrical designs are found in the rotor and

stator configurations of figures 3.1-4 (a), (b), (c) and (d). The remaining components that make up the complete generators are considered to be workhorse parts relative to the electrical designs.

The stators are hermetically sealed to permit operation without subjecting the stator components to corrosion damage by either air external to the generator or by potassium vapor of the rotor cavity. The bore seal in the radial gaps between the rotors and stators is high purity aluminum oxide. The stator magnetic frames consist of the iron-cobalt magnetic alloy, Hiperco-27. Electrical conductors are of Inconel-clad silver and insulated with Anadur (glass fibers). The rotors are composed of solid forgings of high-strength steel that have the necessary magnetic properties. Electrical power is taken out of the generators through bus rings and terminals located on the antidrive ends.

Stator coolant enters and leaves through toroidal shaped manifolds located on the stator frame outside diameter. The manifolds are bonded directly to the stator frame.

Rotor coolant enters the antidrive end and discharges out the drive end with the drive-end bearing lubricant. A special rotor-cavity scavenge system is included as part of each rotor flow circuit.

Pivoted-pad journal bearings support the rotors. The bearing lubricant is separated from the rotor cavities by non-contact viscosity screw-pump seals (viscoseals) or dynamic slinger seals. The rotor cavities are further isolated by Holweck type, molecular vacuum pumps between the viscoseals and rotor cavity.

On the illustrations shown on figure 3.1-4, a drive-end shaft seal-to-space is not shown. All that is required is a low leakage barrier or seal to separate the generator bearings from the turbine bearings. The two bearing cavities would be connected by a hermetic shroud. The actual turbine and alternator frames would be connected by an open, cold-frame fixture.

Materials used for the various parts of the alternator electrical design are discussed in reference 6.

The outer mounting flanges, which double as the hermetic shrouds for the alternators and stators, act as the support structure. The full weight of each generator is carried through two center of gravity mounting pads, one on each side at the horizontal centerline.

One of the features of the generators is the disassembly capability. Breaking the bolted joint on the antidrive end bell and the top weld (near the C.G. mounting blocks) is all that is required to permit removal of the stator assembly intact, complete with bore seal, terminals, and cooling ducts. This feature is included to permit, if necessary, complete separation of the potassium-exposed rotor cavity from the stator cavity prior to breaking any of the hermetic (potassium exposed) seals separating

LEGEND FOR FIGURE 3.1-4(a), (b), (c), and (d)

1. Stator Frame	12. Windage Shield
2. Armature Windings	13. Holweck Pumps
3. Armature Crossover Connections	14. Liquid Shaft Seals
4. Bus Rings	15. Seal Drains to Scavenge System
5. Terminals	16. Journal Bearing
6. Field Coil Windings	17. Thrust Bearing
7. Field Coil Insulation and Heat Shunt	18. Rotor Coolant Inlet and Scavenge System
8. Stator Coolant Ducts	19. Drive Shaft
9. Cooling Duct for Radiation Cooling	20. Cold Frame to Turbine
10. Bore Seal	21. Hermetic Housing to Turbine
11. Bore Seal End Members	22. Center of Gravity Mounting Pad

Figure 3.1-4(a). 400 kVA Alternator

Blank Page

Figure 3.1-4(b). 1200 kVA Alternator

Blank Page

Figure 3.1-4(c). 6000 kVA Alternator

Blank Page

Figure 3.1-4(d). 12,000 kVA Alternator

Blank Page

them. For a discussion of some of the special fabrication procedures typical of these types of alternators, see reference 27.

3.1.3 Generator Design Features

A. Field Coil. The required number of ampere-turns for the electrical design may be obtained by either a large number of low-current turns or a few large-current turns. The choice of which to use depends on a number of factors, e.g., voltage regulator-exciter requirements, heat transfer requirements, material availability, fabrication requirements, etc. As discussed in references 7 and 27, a design utilizing a larger number turns is more difficult to cool because of the high contact resistances between the wires and between the wire and cooling walls. However, this is the type of design which is usually considered first because it can use standard wire sizes and is usually easiest to fabricate.

A comparative analysis was made between the basic field coil designs for the alternators of table 3.1-2 using smaller number of turns, larger-current coils that specifically minimize the hot-spot temperature. As seen by the estimated hot spots in table 3.1-3, there is a definite need for such a configuration in the larger rating machines. The configurations used in the comparative analysis were edge-wound straps of CuBe alloy separated by 0.002 inches of insulation plus 0.002 inches of winding clearance between straps. The outer diameter (edge) of the straps contact electrical insulation (MgO) on the inner diameter of the stator frame cap. A rigid insulation (BeO) fixture surrounds the coil; rigid BeO was chosen since this fixture can be utilized for heat conduction to the sides of the stator frame. This particular strap/coil configuration is also well-suited to vacuum ambient operation since each strap acts as its own heat conduction path and there is only one contact resistance point to consider, that between the strap outside diameter and frame inside diameter.

Table 3.1-3. Field Coil Design Parameters Per Basic Generator Designs of Table 3.1-2

MVA Rating	Max. Field Amps	Max. Field Voltage	Approx. Turns	Amp Turns	Losses (kW)	Resistance (ohms)		Estimated Hot-Spot ^(a) (°F)
						R.T.	Hot	
0.4	21.6	159.6	307	6619	3.4	2.32	7.40	1300
1.2	40.0	167.3	253	10120	6.7	1.31	4.18	1600
6.0	189.6	125.0	114	21616	24.7	0.21	0.66	2000
12.0	436.9	106.7	57	24902	46.6	0.07	0.24	2250

(a) Based on large-turn coil configurations typical of reference 7 in a gas ambient environment but with cooling sink configurations of figure 3.1-4. Temperatures would be greater in a vacuum ambient due to increased contact resistance created by lack of a gas between the wire contact surfaces. They are obviously too high but are shown for comparative purposes to illustrate degree of design difficulty.

The strap thickness, losses, and temperature-rise across the straps (radially) were determined as a function of the number of turns and corresponding current to give the required ampere-turns of table 3.1-3. The results are summarized on figures 3.1-5 through 3.1-8. The radial temperature rise across the straps was calculated per the procedure of reference 26 (Volume I) assuming that all the heat flows radially outward to an 850°F sink. It will be shown later that some heat can flow radially inward with the configuration chosen.

As a result of the comparative analysis, it was concluded that the coil configurations of table 3.1-4 could be applicable to minimum temperature/loss designs. The minimum recommended turns for the 0.4 and 1.2 MVA alternators correspond to a current nearly equivalent to the alternator rated phase current. This is the theoretical limit assuming a current transformer - one turn - would be used as a power source for the voltage regulator-exciter. The 37 maximum turns for the 0.4 MVA alternator corresponds to a design giving about one-half the original (basic) design losses. Actually, according to figure 3.1-5, 50 turns could have been used without incurring any significant losses or temperature rise; the only gain achieved by the 50-turn design over the original 307-turn design would then be the smaller temperature rise for the same losses.

The temperatures in the field coils were calculated for typical designs falling between the recommended turn designs of table 3.1-4; the results are summarized on figures 3.1-9 through 3.1-12. For the 0.4 and 1.2 MVA designs, the BeO insulation was assumed to carry heat away; as a result, it was determined that 21 percent and 28 percent of the field coil losses would choose the "inner" route respectively.

For the 6.0 and 12.0 MVA designs, it was quickly determined that the frame size was sufficiently large to contribute substantially to increasing the hot spot. This is due to the large temperature differential between the coolant and frame walls around the field coil. This is partially the reason for the extremely high hot spots exemplified in table 3.1-3. The solution, as shown by figures 3.1-11 and 3.1-12, is to place the coolant in close proximity with the coil; preferably on the outside diameter so the coil will expand into it and increase thermal contact conductance. For these two designs, it was assumed that all the heat would be flowing radially outward.

The percent contact areas shown on figures 3.1-9 through 3.1-12 represent the assumed areas over which either conductive or radiation heat transfer took place. The contact conductance was fixed at 1.9 BTU per hour per in² per °F; black body radiation was assumed because of the close proximity of the parallel surfaces.

An alternate method for winding the coil (also in a small number of turns) would be to wind the straps flat so their edges would be contacting the sides of the frame. Such a configuration

Table 3.1-4. Recommended Field Coil Configurations
For Minimum Temperature/Loss Designs

MVA Rating	Recommended Turns	Max. Amps	Max. Volts	Losses (kW)	Hot Resistance (ohms)	Strap Thickness (inches)	Estimated Hot-Spot (°F)
0.4	25 (min)	265	6.2	1.64	0.0234	0.046	see figure
	37 (max)	179	12.3	2.20	0.0687	0.023	3.1-9
1.2	13 (min)	780	6.0	4.68	0.0769	0.134	see figure
	50 (max)	202	29.6	5.99	0.1465	0.017	3.1-10
6.0	25 (min)	864	16.5	14.25	0.0191	0.094	see figure
	60 (max)	361	63.0	22.74	0.1740	0.025	3.1-11
12.0	25 (min)	996	28.1	27.99	0.0283	0.078	see figure
	50 (max)	498	81.7	40.69	0.1635	0.027	3.1-12

would give lower temperatures due to shorter thermal paths and two contact edges, would be easier to wind, but would require close dimensional control to assure the coil expanding into the sides of the frame. The differential axial expansion between the Hiperco frame and copper strap is only about 0.020 inches, the same order of magnitude of manufacturing tolerance that can accumulate in a typical design.

The calculated hot spots on figures 3.1-9 through 3.1-12 were generally about 1200°F or lower. These temperatures are within applicable material limitations (by about 200° to 300°F) which would leave some safety factor for design error, off-design conditions of overload, three-phase short, etc. without damaging the coil. Also, the designs and materials should be applicable to coolant temperatures up to possibly 1100°F. The thicknesses of the strap material required varies from about 0.017 to 0.134 inches which is not out of reason for manufacture or coil fabrication. The BeO forms are generally one-fourth to three-eighths inch thick and range in diameter from 18 to 58 inches.

The voltage regulator-exciter (VRE) required for these particular types of coil designs are relatively high-current devices. A comparative study like the above with a VRE included would probably lead to some design compromises with more coil turns and hotter coil temperatures.

B. Stacks. The stator punchings are 0.004-inch-thick Hiperco-27 magnetic alloy, have a semi-closed slot and are assembled into an unwelded armature stack (0.90-inch minimum specified stacking factor) to reduce iron losses. A 0.0002-inch-thick (approximate) layer of plasma-sprayed alumina insulation coating is applied to each side of the punching.

Figure 3.1-5. Field Coil Design Comparison, 0.4 MVA Alternator

Figure 3.1-6 Field Coil Design Comparison, 1.2 MVA Alternator

Figure 3.1-7. Field Coil Design Comparison, 6.0 MVA Alternator

Figure 3.1-8. Field Coil Design Comparison, 12.0 MVA Alternator

Figure 3.1-9. Field Coil Temperatures, 0.4 MVA Alternator

Figure 3.1-10. Field Coil Temperatures, 1.2 MVA Alternator

Figure 3.1-11. Field Coil Temperatures, 6.0 MVA Alternator

Figure 3.1-12. Field Coil Temperatures, 12.0 MVA Alternator

The semi-closed slots are just wide enough at the top to allow the armature wire to be inserted radially; the wider rigid alumina slot liners must be inserted axially. This slot configuration is used to help reduce pole face losses.

The diameter of the punching increases from about 15 inches for the 0.4 MVA alternator to almost 54 inches for the 12.0 MVA alternator. The present limit of commercial rolling facilities for 0.004-inch-thick Hiperco-27 sheet is about 15 inches; therefore, special procurement would be required. Alternatively, stacks made up of segmented punchings might be developed that could utilize 15-inch-wide (or less) sheet. This might prove difficult due to the flexibility and/or other problems associated with handling 0.004-inch-thick stock. The relatively high efficiency of the larger alternators - 92 to 94 percent - suggests the use of thicker stock, say 0.008 inches or more, to reduce development problems. In the past, large stacks of segmented punchings using 0.014-inch-thick stock have been made but handling difficulties were prevalent. Because of this, some designs compromises have been accepted and 0.018-inch-thick stock is now used as a standard "thin" segmented lamination material. In going to 0.004 inches or even 0.008 inches thickness, one would expect formidable development problems.

The interference fit of the stack into the magnetic frame is a critical design area. The interference must be minimized to avoid distorting the laminations and/or over-stressing the magnetic frame; likewise, the fit must be maximized to obtain the maximum heat transfer between the stack and the frame. Because Hiperco-27 has limited strength at the operating temperature, special consideration must be given to methods of maximizing the heat conductance without an excessive interference fit. Since the frame acts as a heat sink to the stacks, the thermal conductance will rise as the fit (or stack temperature) increases; this minimizes the possibility of over-heating in the stack.

The configuration of the heat sink on the frame is important to the ease of fabrication and allowable material hot-spot temperature. The cooling manifold configuration shown on figure 3.1-4 is relatively easy to fabricate with high reliability but suffers from being farther from the heat source than a configuration that has cooling within the frame. A configuration with cooling in the frame reduces the hot-spot temperature but usually is more difficult to manufacture and still maintain high reliability. For example, axial cooling tubes through the frame (as in reference 27) place the coolant close to the stacks but requires a considerable number of connecting welds at the tubes and a precise dimensional controlling fixture for brazing the tubes in the frame. In addition, the tubes usually must dissect the field coil making it difficult to wind the coil or replace it in a finished generator.

A comparative study was made using the 400 kVA design to determine the relative cooling merits of the manifold configuration shown to those of putting axial cooling tubes through the

frame. The manifolds increase the hot-spot temperature approximately 150°F over that possible with axial cooling tubes every 3 degrees on the stack outside diameter. Reducing the number of tubes to every 30 degrees on the stack outside diameter (to reduce tube weld connections) increased the hot spot about 400°F over those with the manifolds.

C. Armature Windings. The armature windings are three-phase, wye-connected; effective phase belt grouping is 60 degrees. Coil pitch is 0.67 to eliminate third harmonic voltages. The armature slots are not skewed to facilitate winding. An alternate choice of 120-degree phase belts was not used primarily to minimize weight, total losses, and armature winding hot-spot temperatures at the expense of adding joints to the windings.

The coil end extensions are small due to the two-thirds pitch used with the 60-degree phase belts. Even though this keeps end extension losses to a minimum, the temperatures in the end extensions can be expected to be the design limiting hot spots. This is because the end extensions are at the furthest point from the heat sink and heat conductance across the slots to the stacks is generally poor.

Design relative to the armature slot insulation material and slot insulation configuration (slot cell) is critical because two contrary purposes must be served; that of providing high electrical resistance between phases and ground while also having low thermal resistance to act as an effective heat path for conducting the armature losses out of the wire. Since flexible, high-temperature insulations have not yet been developed for these types of applications, a rigid electrical insulation system is required which results in a high thermal resistance between the wire and the stator stacks. The reason for this is the high thermal contact-resistance between the rigid insulation and the wire and stacks. Whenever the ambient within the slot is a (space) vacuum, the contact conductance is even more critical since the gas usually represents the major heat carrier between the slot components. Since the contact conductance is usually less than the thermal conductance of a conducting gas ambient (such as argon), the armature wire will be much hotter for the vacuum ambient than for a gas ambient.

Since contact resistance is a major consideration in the slot design, analytical predictions of the total slot-configuration thermal resistance are unavoidably inconsistent. As a result, a variety of situations must be examined to bracket the design cases between the most conservative approach and a more optimistic approach. This is because analytical methods to predict contact conductance are imperfect at best. Conservatism must be used in all design cases because of these uncertainties involved in predicting contact resistance and in reproducing it in the actual slot configurations.

The procedure for the slot design study was (1) to vary the current density in the wire (varies heat flux), (2) to vary

the general slot thermal contact resistance to determine a range of conditions to satisfy the design limitations, and (3) to examine various slot configurations to find those falling within the range.

Early in the parametric study, reference 6, the current density was varied to determine applicable values that limited stator winding temperatures without adding too much weight or requiring too high thermal conductance in the slot configuration. The results are summarized in table 3.1-5 for the two smaller ratings. The current densities shown are approximate; the actual values can vary as much as several hundred amps per square inch to adjust for rated current in standard wire sizes. The reduction in temperatures at the lower current densities illustrates the gains that can be achieved using this approach to minimize temperatures.

Using a 12-MVA design as a reference case, a generalized study was made of the effect of slot conductance on the average winding temperature. The results shown on figure 3.1-13 indicate that there is a definite range below which winding temperatures are excessive and above which further temperature reductions are insignificant. Note that the current density used was high compared to what was used in the parametric studies and conceptual designs so that the winding temperatures shown are greater than normally allowed. In addition, radiation of heat off the winding end turns, which is usually considered, was neglected in the reference case shown. Inclusion of the radiation would lower and flatten the curve (except near zero equivalent conductance).

Based on the above results, a number of slot cell configurations were analyzed to determine which of them would fall in a range of approximately 0.5 to 1.0 watts per square inch per degree C. The results are shown on figure 3.1-14 with the case numbers corresponding to the paragraphs below. The two slot configurations considered are illustrated on figure 3.1-15. Rather than attempt to determine the contact resistance for the various cases examined, a single approximate value, with appropriate tolerance, was assumed and kept constant throughout except where noted. Because of the tolerance, ranges of uncertainty are shown on the figure as shaded areas. The results are shown separately for 0.4 to 1.2 and the 6.0 to 12.0 MVA generators because of the different slot manufacturing clearances that were used on the smaller and larger machines respectively.

Case #1A: Slot configuration figure 3.1-15 (a); Al_2O_3 slot liner ($k = 0.170$ watts/in $^\circ\text{C}$); void ambient is space vacuum; ignore radiant heat transfer between wire and slot cell and between slot cell and stator tooth; assume there is contact (random) over 5 percent of the area of the wire, cell and tooth; contact conductance = ± 0.5 watts/in 2 $^\circ\text{C}$

Case #1B: Same as Case 1A except contact area is 50 percent.

Table 3.1-5. Current Density Study, Average Winding Temperature

Approximate Current Density (amps/in ²)	Coolant Temperature		
	500°F	800°F	1100°F
0.4 MVA (a)			
9000	1200	1600	1920
7000	960	--	--
6000	--	1270	1360
1.2 MVA (b)			
9000	1300	1680	1930
7000	1000	--	--
6000	--	1370	1330
<p>(a) All are for 24,000 rpm, 2000 Hz, 1000 volts except 1100°F 6000 amps/in² case is at 15,000 rpm. Equivalent slot conductance was 0.11, 0.17 and 0.18 watts/in²°C for the three coolant temperatures respectively.</p> <p>(b) All are for 15,000 rpm, 2000 Hz, 1000 volts except 1100°F, 6000 amp/in² case is at 10,000 rpm. Equivalent slot conductance was same on on 0.4-MVA designs.</p>

Figure 3.1-13. Effect of Slot Conductance on Average Winding Temperature

Figure 3.1-14. Comparison of Slot Conductance

Figure 3.1-15. Slot Configuration

Case #1C: Same as Case 1A except the void ambient is gas with a conductivity of 0.0011 watts/in °C (argon).

Case #1D: Same as Case 1C except contact area is 50 percent.

Case #2A: Same as Case 1A except slot void is filled with a potting compound ($k = 0.020$ watts/in °C) and contact area is 50 percent across the thermal expansion crack between the slot cell and tooth. The expansion crack is 0.000066 inches wide and contains space vacuum.

Case #2B: Same as Case 2A except that a conducting gas replaces the space vacuum in the thermal expansion crack.

Case #2C: Same as Case 2B except that contact area is assumed to be zero percent in the thermal expansion crack.

Case #2D: Same as Case 2A except radiation across the thermal expansion crack is included based on the calculated temperature differences in the conceptual designs. The conductances equivalent to the radiant heat transfer mode were $0.0793 + 0.0024$ and $0.0961 + 0.0026$ watts/in²°C for the 0.4 to 1.2 and 6.0 to 12.0 MVA designs respectively.

Case #3 : Same as Case 2D except that BeO is substituted as the slot liner. The thickness of the slot liner is increased 50 percent to account for the lower dielectric strength of the BeO.

Case #4A: Slot configuration figure 3.1-15 (b); Boron nitride slot liner ($k = 1.60$ watts/in°C); void ambient is space vacuum; include radiation between wire, slot and tooth; assume 50 percent contact area and contact conductance = $1.6 + 0.8$ watts/in²°C; reduce the slot clearance to one-fourth of original since wedges do not require as much clearance for manufacturing tolerance build-up. The increase in contact conductance corresponds to increasing contact pressure from 50 to 1000 psi, and according to Graff*, contact conductance will increase as the 0.16 power (approximately) with pressure.

Case #4B: Same as Case 4A except the thermal contact resistance between the wedges is ignored; it's treated as a solid piece.

Comparison of the results on figure 3.1-14 indicates the improvement in heat transfer capability that occurs when a gas is used in the stator cavity as an ambient. Combine this with a filler material such as potting compound and the improvement becomes significant. In a vacuum ambient, most of the equivalent conductances examined were between 0.2 and 0.4 watts per square inch per degree C which, although this is low, it is not sufficiently low to prevent cooling the armature coils. Generally,

*Machine Design, September 15, 1960, pp. 166-172.

a filler material is required to put the conductance at the upper end.

In the event a filler material such as a potting compound is not permitted because of its integrity over long life operation, one must comply with the lower conductance values or change to a different slot configuration such as on figure 3.1-15 (b). It should also be pointed out that the results of the boron nitride cases (4A and 4B) are also applicable to an Al_2O_3 or BeO slot liner since the contact conductance overshadows the material conductivity enough to make changes in material conductivity insignificant to the outcome.

In addition, one can (1) include heat shunts on the end turns to shorten thermal paths and increase the area of conductance, thereby increasing the effective value or one can (2) include radiation off the end turns (to the cold frame surrounding them) as an additional source of heat transfer. For the 800°F coolant conceptual design cases, including radiation off the top surface of the end turns was sufficient to reduce the winding temperatures to acceptable levels even at the lower values of slot conductance. This is illustrated by the temperature (average and maximum "hot spot") plots on figure 3.1-16. Comparison of these plots with figure 3.1-13 points out the previously mentioned flattening of the curve from 0.2 to 0.5 watts per square inch per degree C. The temperatures for the 6 MVA design are slightly higher than they should be, but for analytical purposes, are all right. The corresponding pertinent temperatures for the rest of the stator stack at an equivalent slot conductance of 0.2 watts per square inch per degree C are shown on figures 3.1-17 through 3.1-20.

The effect of the temperature rise and therefore losses on efficiency above a conductance of 0.2 was found to be less than 0.1 percent points in efficiency.

The conclusions to be drawn from the above comparison cases are as follows:

- a) A potting compound filler with a gas ambient gives conductance values approximately three times greater than in a vacuum with filler or in a gas without filler and approximately six times greater than in a vacuum without filler.
- b) Suitable cooling schemes in vacuum without filler are possible but must rely on additional heat transfer other than across the slot components.
- c) Radiant heat transfer off the end turns adds sufficient cooling capacity to minimize winding temperatures even for minimum slot conductances. At higher coolant temperatures, say 1100°F, this may not be applicable and additional features such as heat shunts directly from the windings to the coolant may be

Figure 3.1-16. Conceptual Design Stator Wire Temperatures

Figure 3.1-17. Armature Operating Temperatures, 0.4 MVA Conceptual Designs

Figure 3.1-18. Armature Operating Temperatures, 1.2 MVA Conceptual Designs

Figure 3.1-19. Armature Operating Temperatures, 6.0 MVA Conceptual Designs

Figure 3.1-20. Armature Operating Temperatures, 12.0 MVA Conceptual Designs

required or alternatively, a higher temperature wire such as CuBe alloy may be used.

- d) Contact resistance is the greatest barrier to heat transfer across the slot; any methods or configurations to reduce it will result in proportionate gains in heat transfer capacity up to a point; thereafter, the heat transfer capacity of the slot does not further reduce hot-spot temperatures. Flexible slot insulation would be a recommended method for increasing the conductance; changing the rigid insulations from Al_2O_3 to BeO or boron nitride has an insignificant effect on the overall slot conductance because of the predominating contact resistance.

D. Stator Winding Connections. There are four to six bus rings on the antdrive end of each generator; three are for line connections and one to three are for neutral connections to the phases. The parallel phase groups of the armature are connected to each bus ring in such a manner that bus ring copper losses are held to a minimum which in turn permits the rings to be cooled by radiation to the stator cavity walls. The maximum bus ring temperature can be expected to be less than 1100°F with an 800°F sink temperature on the end-bell wall.

There are as few as one and as many as four terminals per bus ring located such that each carries not more than 2000 amperes full-load current. Because of the temperatures brought on by the high current densities and because of the limited space available, standard terminal feed-throughs can not be used. Special terminal studs (as shown schematically in figure 3.1-4) are required.

The conductors through the terminal feed-throughs are shown as hollow tubes on the larger machines. This configuration is desirable, at some compromise to feed-through insulator design, to have sufficient cross-sectional area to permit the tubes to be self-cooled by radiation and/or natural convection. The tubes can be nickel plated and black oxide coated to obtain an emissivity of at least 0.4 on the outer diameters. Radiation cooling alone limits the conductor temperature to less than 1000°F at full-load current in an 800°F ambient.

E. Bore Seal. One of the more vital components in the generator is the ceramic bore seal which separates the rotor cavity from the stator cavity. This separation is necessary because the stator components are incompatible with the potassium vapor in the rotor cavity. Since the bore seal is in a continuing state of development, the bore seal design for the conceptual design generators was not finalized; each design must be treated individually when complete operating/fabrication conditions are specified or known.

The bore seal proposed for 800°F coolant generators is a 99.8% pure alumina cylinder with Cb-1Zr refractory alloy metal

end members. Since the bore seal will most likely be hermetically joined to non-refractory material in the generator, the use of refractory end members requires that an equally sound transition joint also be provided. Seal-weld joints are made during final assembly by electron-beam welding or TIG welding. For higher temperature designs, a 99.8% pure BeO cylinder with T-111 alloy metal end members would be required. The various development states of these bore seal concepts are summarized in Appendix A.

As indicated previously, final designs of the bore seals were not made because of the individual nature of each design relative to its operating requirements or the various conditions of manufacture it is subject to prior to use; the schematic designs shown on figure 3.1-4 are only meant to be representative. There are a few general rules that should be followed or considered that are usually common to all the designs. They are:

- a) The bore seal and end members must accommodate either internal or external pressurization to permit helium leak testing, gas filled stator cavities, or pressure build-up from out-gassing products of the stator materials.
- b) The end members must accommodate the displacements between the bore seal and stator frame due to pressure and differential thermal expansion - both radial and axial. This is usually difficult since, for the large bore seals meeting requirement a) above, the end members must be stiff and therefore become highly stressed when deflected sufficiently to accommodate the displacements. As a result, the maximum stress usually occurs in the metal end member and is caused by these contrary requirements. The thermal conditions must be uniform soaking temperatures and gradients during operation or manufacture.
- c) The bore seal and end members must accommodate the shock, vibration, etc. conditions that the alternator will be subjected to during manufacture and operation. The critical buckling or flattening pressure and the resonant vibration conditions must be considered.
- d) The joints should be of the sandwich type as shown in Appendix A to utilize past development as much as possible.

As a result of more detailed studies on NASA contracts NAS 3-10933 and NAS 3-10934, a proposed bore seal end member design was defined that would fulfill the contrary needs of rigidity and flexibility. The solution was to provide an additional support fixture for the metal end members and bore seal ends such that it limits the possible pressure-caused deflections while still having ample flexibility for thermal expansions.

In addition to alleviating the bore seal end member design problems, the proposed configuration permits more accurate control of the alignment of the bore seal in the radial gap. The net effect of this is that less radial gap is required for assembly tolerances and then (1) the gap may be decreased, (2) the rotor may be allowed to creep more, or (3) the bore seal may be thickened.

The diameters of the bore seals for the various ratings can be scaled from figure 3.1-4 to sufficient accuracy to forecast developmental needs in the area of bore seal sizes. The thicknesses required for the alternator designs of table 3.1-2 were determined to be greater than the minimum thicknesses that could be fabricated by suppliers proficient in this field (see table 3.1-6). In other words, the minimum permissible bore seal thickness is greater than the possible minimum; therefore, fabrication development problems should be reduced somewhat so far as bore seal thickness goes. Because the brazing alloy in the joints might be one of the active-metal alloys, reference Appendix A, there is a limit to the size of the bore seal that can be brazed with the presently available brazing facilities in the United States. The reader is referred to reference 28 for a more detail discussion of these design considerations.

There are several alternatives to using the bore seals discussed above; each has their own development problems. For example, the rotor and stator cavity may be opened to space, thereby obviating the need for a potassium resistant, leak-tight bore seal; a bore "barrier" might be all that is required to prevent the potassium from having direct access to the stator cavity. The barrier could be a lower purity ceramic brazed with conventional materials.

Alternatively, the bore seal could be eliminated if a potassium resistant electrical insulation and conductor system could be developed. Work on contracts AF33(657)10701(29), AF33(615)1360(30) and AF33(615)3528 has been carried on at Westinghouse Aerospace Electrical Division for the purposes of developing such systems. Studies to date have identified potassium resistant materials that can be used, has determined that voltages must be low - of the order of 120 volts rms - and that considerable problems remain before working systems can be developed; the reader is referred to the references for additional details. Ion phenomena, identified during contract AF33(615)3528, on transformers operating in potassium vapor indicate several possible problem areas that may be more significant than originally anticipated, not only on designs without a bore seal but on designs with a bore seal.

F. Rotor Design.

1. Limitations of Rotor Material. A comparison of several designs with rotor material properties as the variant indicated that H-11 steel was the superior material. An interpretation of the available strength data for H-11 steel from Technical

Table 3.1-6. Bore Seal Thickness Summary

MVA Rating	Original Estimated Minimum Bore Seal Thickness per Manu- facturer's Capabilities (ref. 1) (inches)	Permissible Bore Seal Thickness per Alternator Designs of Design Study (ref. section 3.1.4) (inches)
0.4	0.065 inches	0.070 inches
1.2	0.080 inches	0.102 inches
6.0	0.129 inches	0.212 inches
12.0	0.175 inches	0.290 inches

Report No.'s AFAPL-TR-66-127⁽³¹⁾ and NASA-CR-54091⁽³²⁾ is graphically summarized in figure 3.1-21. The yield and ultimate strength given in figure 3.1-21 are the nominal values for these properties. Only a limited amount of creep data is available for H-11 steel and considerable extrapolation of the data was necessary to obtain the creep information for a five-year time period. Therefore, the creep strength given in figure 3.1-21 can only be considered a "best appraisal" based upon a limited amount of short-time creep data.

The stress limitations which H-11 steel impose upon a rotor is summarized in figure 3.1-22. These limitations are given in terms of an allowable stress intensity at design speed and are based upon the following considerations:

- a) The rotor shall be capable of a 20 percent overspeed without a catastrophic failure. Thus, at a 20 percent overspeed, the stress intensity within the rotor shall be less than the minimum ultimate tensile strength of the material. Eighty-seven percent of the nominal value is used as a criterion for the minimum ultimate strength.
- b) The stress intensity within the rotor shall be less than minimum 0.2% offset yield-strength of the material; 72% of the nominal value is used as a criterion for the minimum 0.2% offset yield strength.
- c) The third criterion for an acceptable design is that the overall creep growth within the rotor at the end of five years operation will not cause a rub between the pole tips and bore seal. It is anticipated that the initial dimensions of a typical alternator will permit a maximum of 0.33% creep strain which means that the highly stressed portions of the rotor can have a creep strain of about 0.5%.

Figure 3.1-21. Material Strengths of H-11 Steel at Rockwell C45

Figure 3.1-22. Allowable Material Strengths of H-11 Steel at Rockwell C45

2. Rotor Stress for Conceptual Designs. The stresses, i.e., maximum and minimum principal stress, are used to calculate an equivalent stress intensity which is then compared with the uniaxial tensile data given by figure 3.1-22 to determine the acceptance of the designs. The procedures outlined in Appendix B were used to calculate the stress distribution and equivalent stress intensities within the rotor. Four stress profiles are graphically illustrated in figures 3.1-23, 3.1-25, and 3.1-26 for the 0.4, 6.0, and 12.0 MVA alternators, respectively. These stress profiles represent the following conditions:

- a) The radial stress distribution which would exist within the rotor core if the centrifugal forces on the poles were uniformly distributed around the outer boundary of the rotor core.
- b) The radial stress distribution that exists at the mid plane under the poles.
- c) The radial stress distribution that exists at the mid plane between the poles.
- d) The average tangential and radial stress with respect to the radius of the rotor core that exists around the core.

These stress profiles plus the radial stress distribution at several planes around the core are given in figure 3.1-24 for the 1.2-MVA design. The maximum stress intensity occurs at the mid plane under the poles. There, the biaxial stresses are converted into an equivalent stress so a comparison with uniaxial tensile data from figure 3.1-22 can be made.

3. Comparison of Actual Stress Intensities With the Allowable Stress for H-11 Steel. The maximum stress intensity for each of the conceptual designs is given in table 3.1-7. The comparison indicates the actual stress intensities within the rotor are well below the allowable stress for the H-11 steel at 800°F.

4. Reduction of Pole Face Losses. Only one technique for reducing the pole face losses was considered during screening of the designs in the parametric study (reference 6). This technique consisted of slotting the pole faces with grooves in a direction parallel to the motion of the rotor. With grooves that are 0.005 inches wide by 0.10 inches deep at a pitch of about 30 grooves per inch, the resistance of the eddy-current paths was increased by a factor of approximately 2.8 when compared with the resistance of solid pole faces. For most of the designs investigated, these grooves provided a 29 to 40 percent decrease of the pole face losses when compared with losses associated with solid pole faces.

The losses within the pole face from the standpoint of material properties are primarily a function of $(\rho\mu)^{-\frac{1}{2}}$ where ρ equals the effective resistivity of the material along the eddy-

Figure 3.1-23(a). Rotor Stress at Uniform Load

Figure 3.1-23(b). Rotor Stress at $\theta = 0^\circ$

Figure 3.1-23(c). Rotor Stress at $\theta = 36^\circ$

Figure 3.1-23(d). Rotor Stress (average) as a function of θ

Figure 3.1-24(a). Rotor Stress at Uniform Load

Figure 3.1-24(b). Rotor Stress at $\theta = 0^\circ$

Figure 3.1-24(c). Rotor Stress at $\theta = 4^\circ$

Figure 3.1-24(d). Rotor Stress at $\theta = 8^\circ$

Figure 3.1-24(e). Rotor Stress at $\theta = 12^\circ$

Figure 3.1-24(f). Rotor Stress at $\theta = 16^\circ$

Figure 3.1-24(g). Rotor Stress at $\theta = 20^\circ$

Figure 3.1-24(h). Rotor Stress at $\theta = 24^\circ$

Figure 3.1-24(i). Rotor Stress at $\theta = 28^\circ$

Figure 3.1-24(j). Rotor Stress at $\theta = 32^\circ$

Figure 3.1-24(k). Rotor Stress at $\theta = 36^\circ$

Figure 3.1-25(a). Rotor Stress at Uniform Load

Figure 3.1-25(b). Rotor Stress at $\theta = 0^\circ$

Figure 3.1-25(c). Rotor Stress at $\theta = 22.5^\circ$

Figure 3.1-25(d). Rotor Stress (average) as a Function of θ

Figure 3.1-26(a). Rotor Stress at Uniform Load

Figure 3.1-26(b). Rotor Stress at $\theta = 0^\circ$

Figure 3.1-26(c). Rotor Stress at $\theta = 18^\circ$

Figure 3.1-26(d). Rotor Stress (average) as a Function of θ

Table 3.1-7. Comparison of Rotor Stresses

Rating (MVA)	Temp. (°F)	Maximum Stress Intensity (psi)	Percent of Minimum 0.2% offset Yield Strength	Percent of Allowable Stress Intensity for 0.5% Creep Strain in 5-yr.
0.4	800	52500	53%	58%
1.2	800	71200	72%	79%
6.0	800	67250	68%	74%
12.0	800	79300	80%	88%

current path and μ equals the effective permeability of the material when a small time varying induction is imposed upon the steady induction.

The H-11 steel offers superior dc magnetic and strength properties for the rotor. Unfortunately, this steel offers a very low ac magnetic permeability when subjected to the time varying inductions associated with the pole tips; therefore, this material must be considered inferior from the standpoint of pole face loss reduction. A comparison of the properties of interest for H-11 steel with the properties of superior steels is given in tables 3.1-8 and 3.1-9. It can be noted from the tables that the resistivity of the materials are about the same magnitude. However, a comparison of the effectivity permeability of the materials indicates the silicon steels are a much better material from the standpoint of pole face losses.

Several pole face configurations were investigated for the alternator conceptual designs and the pole face losses for each rating and configuration are presented in table 3.1-10. It can be noted from the table that the pole face losses are excessive when provisions for the reduction of these losses are not incorporated on the pole tip. Without provisions for reducing the pole face losses, a significant weight penalty is imposed upon the system due to the decrease in performance of the alternator. A significant increase of alternator efficiency can be achieved by slotting the pole face; however, the losses are still large compared with losses associated with laminated pole tips. Laminating the pole tips reduces the losses by 31 to 46.6 folds and the system weight penalty associated with these losses is very small.

Table 3.1-8. Comparison of Rotor Resistivity

	H-11 Steel	Non-grain-oriented 4-1/4% Silicon Steel	Grain-oriented 2-1/4% Silicon Steel
Bulk material	78.7	108	69
Effective Resistivity of Slotted poles	304	416	-
Effective Resistivity of laminated pole piece, 0.010 inch sheet by 0.25 inch depth	670	918	586

Table 3.1-9. Comparison of Rotor Effective Permeability (c.g.s.)

Amplitude of time varying induction on surface of pole face (gausses)	H-11 Steel	Non-grain-oriented 4-1/4% Silicon Steel	Grain-oriented 2-1/4% Silicon Steel
0	1.8	350	1050
130	7.1	520	1560
182	8.1	580	1740
242	9.0	642	1826
394	11.0	739	2217
578	12.8	803	2409
802	14.7	885	2655

In addition to the efficiency and weight considerations, the heat flux within the body of the pole is reduced and a complex cooling scheme is not required to maintain a reasonable pole face temperature. Without laminated pole tips, it would be very difficult to maintain a pole face temperature less than 950°F* with an 800°F coolant, but with laminated pole faces, it is not necessary to have coolant passage in the main body of the poles.

5. Rotor Cooling Scheme. A scheme which offers distinct advantages for cooling the rotor is illustrated in figure 3.1-4. With this scheme, the coolant enters the antidrive-end of the rotor along the axis of rotation. The flow is then diverted in a radial direction through the shaft extension and into a manifold which serves as a distribution duct for the coolant passages in the main body of the rotor. A similar manifold on the outlet side of the rotor is used to divert the coolant into the drive end bearing discharge chamber. A volute acts to convert the angular velocity of the fluid leaving the rotor to static pressure which results in a net pressure rise through the rotor. The work required to accomplish this is usually only a few hundred watts.

Each pole on the rotor is served by one of the parallel passages through the rotor. An analysis of this type of rotor cooling concept is given in reference 7. This scheme offers distinct advantages for cooling a solid rotor since the heat conduction paths between the pole faces and the coolant passages are short and external pumping is not required to circulate the coolant through the rotor. The complications associated with this cooling scheme are due to the high centripetal acceleration which acts upon the fluid in the passage. In effect, the coolant passages form a centrifuge which is capable of separating the solid materials that are transported by the coolant. The only apparent practical solution to this dilemma is to design the coolant passages so that the accumulation of the solid materials will not close or suppress the flow the coolant. This can be accomplished by using extra passages in the body of the pole to serve as an accumulator for the heavy material. For designs which do not require coolant flow within the poles, such as those with laminated pole tips, an accumulator can be incorporated within the manifold on each end of the rotor.

The large centripetal acceleration on the coolant also produces large pressures within the coolant passages. Examples given in reference 7 show that the 12-MVA alternator would have 10,900 psi pressure within the passages near the pole tips. In general, the magnitude of the pressure requires a welded construction for the passage even when coolant passages are not used

*Limiting-use temperature of the H-11 steel in the pole and pole tip vicinity is about 950°F based on both strength and magnetic considerations.

Table 3.1-10. Comparison of Pole Face Losses and Effects

Rating, MVA	0.40	1.20	6.00	12.00	Pole Tip Configuration
Rating, MW (Electrical)	0.340	1.020	5.100	10.200	
Efficiency, Excluding Pole Face Losses	92.51%	94.08%	93.95%	94.23%	
Pole Face Losses, Watts	8,145	26,470	120,700	129,700	Solid Pole Tips of H-11 Steel
Efficiency, including Pole Face Losses	90.51%	91.84%	91.91%	93.11%	
Losses per unit area, Watts/in ²	96.38	149.91	189.89	95.38	
Weight penalty associated with pole face losses, pounds	117	359	1644	1780	
Temp. drop required to conduct heat from pole tips to rotor core, °F	648	1476	3880	2590	
Pole Face Losses, Watts	5,373	18,650	68,580	78,460	Grooved Pole Tips, 0.005 inches wide by 0.10 inches deep with 30 grooves per inch in H-11 steel
Efficiency, including pole face losses	91.18%	92.49%	92.78%	93.55%	
Losses per unit area, Watts/in ²	63.59	105.59	107.94	57.69	
Weight penalty associated with pole face losses, pounds	77	253	935	1073	
Temp. drop required to conduct heat from pole tips to rotor core, °F	427	1040	2400	1569	
Pole Face Losses, Watts	371	1230	3648	4373	Laminated, 4.25% Silicon Steel, non-grain-oriented, Depth of lamination = 0.5 inches, Thickness = 0.010 inches.
Efficiency, including pole face losses	92.42 %	93.97 %	93.89 %	94.19 %	
Losses per unit area, Watts/in ²	4.392	6.966	5.741	3.215	
Weight penalty associated with pole face losses, pounds	5.3	16.7	49.8	59.9	
Temp. drop required to conduct heat from pole tips to rotor core, °F	29.6	68.6	117.1	87.4	
Pole Face Losses, Watts	198	790	2,338	2,921	Laminated, 2.25% silicon steel, grain-oriented, Depth of laminations = 0.5 inches, Thickness = 0.010 inches.
Efficiency, including pole face losses	92.47 %	94.01 %	93.91 %	94.20 %	
Losses per unit area, Watts/in ²	3.095	4.844	4.080	2.275	
Weight penalty associated with pole face losses, pounds	3.7	11.6	35.3	42.4	
Temp. drop required to conduct heat from pole tips to rotor core, °F	20.8	46.6	84.5	61.8	

in the poles. Passages will not be required within the pole for the conceptual designs if laminated pole tips are used. For this case, the passages would be drilled through the rotor core on a diameter about one-half of an inch below the outer diameter of the rotor core; the resulting pole face temperatures are given in table 3.1-11.

6. Rotor Cavity Scavenge System. One of the unique features of the generators is the rotor cavity scavenge system. This system acts to clear the rotor cavity of liquid during start-up and keep it clear during operation. It is designed as an integral part of the generator shaft seal system and works automatically without external controls. Tests which demonstrated and verified the performance of the system were completed and are reported in detail in Appendix D of reference 27.

G. Bearings and Rotor Dynamics. The bearings are four-pad, pivoted pad hydrodynamic journal bearings. The pads use a ball pivot to accommodate angular misalignment. Solid carbides were chosen for the journal, pad wear surfaces, pivot ball, and socket. Special mounts are used to compensate for the relative thermal expansion between the solid carbide pieces and the steel housings and stub shafts.

A circulating flow of potassium through each flooded bearing cavity acts as the coolant and lubricant. Special pressurized lubricant feeds in the pads are not required since the hydrodynamic action of the pads forms the wedging phenomenon necessary for full film lubrication. Volutees are provided on the discharge side of the bearing to collect the lubricant and convert its tangential velocity into static pressure. The volute on the drive end also acts as the collecting volute for the rotor coolant flow. Because of these volutes, a net pressure rise occurs

Table 3.1-11. Pole Tip Temperatures, Laminated Poles

Rating (MVA)	Pole Face Losses (watts)	Coolant Temperature (°F)	Pole Face Temperature (°F)
12	2921	800	862
6	2338	800	885
1.2	790	800	847
0.4	198	800	821

through the bearings. See reference 6, Appendix A for a more complete discussion of the bearing design and rotor dynamics.

H. Shaft Seals. The shaft seals are of the non-contact type that form a barrier from the liquid being sealed. They consist of liquid screw-pump seals (viscoseals), liquid dynamic slinger seals, and vapor shaft seals in the form of Holweck type molecular vacuum pumps.* The liquid seals prevent liquid from escaping the bearing cavity whenever the seal pumping head becomes greater than the pressure between the bearing cavity and the rotor cavity. At that condition, there is established within the seal, a liquid-to-saturated-vapor interface past which only vapors can leak. These saturated vapors are prevented from entering the rotor cavity by the Holweck pump vapor seals. The vapor seals accomplish this by pumping the rotor cavity down to a lower pressure such that the vapors must be superheated at the temperature conditions found in the rotor cavity. Thus, any wet vapors from the liquid seal interface eventually passing into the rotor cavity will have become dry in the process.

Because of the mixing which can occur in both the liquid seals and Holweck pumps, sealing of dissimilar fluids in the seals is not possible.

A second beneficial effect is derived from the reduced rotor cavity pressure achieved by the vapor seals; the windage losses are reduced. This is because windage losses vary exponentially with the pressure and temperature (i.e., density changes). Studies have shown that the performance of the vapor seal in the applicable flow regimes can be predicted by an approximate analysis of a simplified flow model. The approximate analysis was verified by laboratory tests using argon gas to simulate potassium vapor. Based on this approximate analysis, three stages of Holweck pumps would probably be required as shown of figure 3.1-4. See reference 2 for the discussion of basic shaft seal designs.

Both the bearing losses and shaft seal losses are included in the efficiency figures of table 3.1-2.

3.1.4 Electrical Design Considerations

Designs representative of optimum generator configurations were selected from results of the second cycle parametric studies(6) for more detail analysis. These designs are shown in table 3.1-12. Analyses were focused primarily on reducing rotor pole face losses

*A Holweck molecular vacuum pump operates on the same principle as a diffusion pump except that the momentum is imparted to the molecules by a rotating cylinder rather than by a fluid. The configuration is that of a smooth shaft (or cylinder) running in a close fitting (e.g., 2 to 3 times the bearing clearance), helical threaded housing.

Table 3.1-12. Parametric Study Designs

Rating (MVA)	Speed (rpm)	Gen. Weight (lbs)	Eff. (%)	Gen. Weight Plus Weight Penalty (lbs)	X_d (P.U.)	Num- ber Poles	Avg. Tan gential Stress (psi)	Prelim. Pole Face Losses (watts)	Radial Gap (in)	No. of Parallel Strands Per Con- ductor	Frame Flux Density (kL/in ²)
0.4	30000	329	91.2	769	1.09	8	65560	2724	0.100	1	70
1.2	19200	1151	92.1	2251	1.14	10	55708	3989	0.125	1	70
6.0	12000	5317	92.4	9717	1.14	12	74571	103241	0.229	4	70
12.0	8000	16619	93.5	23719	1.21	18	63901	66511	0.275	4	70

in those cases where pole face loss was unacceptably high while keeping any corresponding system weight penalties to a minimum. Generator direct axis synchronous reactance (X_d) and generator radial gap were varied to determine the optimum values regarding system weight and the effect on pole face loss. An important criteria used in selecting final designs was that the number of stator slots must be such as to give an integral slot winding, that is, the number of slots per pole per phase must be an integer. Fractional slot windings (i.e., slots/pole/phase $\neq$ integer) are known to produce even harmonics (and in some cases, sub-harmonics) in addition to the odd harmonics which are present in the magnetomotive force (MMF) of integral slot windings. The armature MMF of a fractional slot winding can greatly increase the load component of pole face loss.

Curve "a" of figure 3.1-27 shows a plot of generators radial gap versus MVA rating as used in the parametric screening studies. Curve "b" shows the optimum radial gap (with regard to system weight and pole face loss) determined from the conceptual design analysis. For the 0.4 MVA rating, increasing the radial gap by 20 percent to reduce pole face loss caused the rotor stress to exceed the maximum allowable stress of the rotor material. Rotor speed was reduced instead to lower rotor pole face losses while maintaining the original gap of 0.100-inch which is the optimum gap regarding system weight for this design.

The increased radial gaps for the 6 and 12 MVA generators also caused the rotor stress to exceed the maximum allowable material stress limit. Therefore, speed was reduced to lower the induced rotor stress and to achieve a required further reduction in pole face losses.

The percent reduction in pole face loss achieved for the four ratings as a result of the increased radial gap and reduced speed are listed in table 3.1-13.

A very conservative value of stator frame flux density (70 kL/in^2) was used in the parametric studies. This value was not changed during the course of the studies in order to prevent a discontinuity in the resulting trends. Results of the conceptual design analysis showed that the frame flux density could be increased to approximately 110 kL/in^2 with only a slight increase in field power requirements while obtaining a significant reduction in system weight. The resulting weight reduction more than offset the weight increase due to speed reduction for the 12 MVA rating and minimized the weight penalty associated with speed reduction for the 0.4 and 6.0 MVA designs.

Efficiency of the 1.2 MVA rating was increased from 92.1 to 93.6 by decreasing the armature winding eddy-current factor. This was accomplished by changing from a single large series conductor to two smaller parallel conductors in the armature. The generator weight increase associated with this change was 13 pounds; whereas, the system weight savings due to increased efficiency was 210 pounds.

Figure 3.1-27. Generator Radial Gap

Table 3.1-13. Percent of Pole Face Loss

Rating (MVA)	Speed Reduction (%)	Increase in Radial Gap (%)	Reduction in Pole Face Loss (%)
0.4	20	0	32
1.2	0	20	66
6.0	18	40	84
12.0	10	56	84

The generator designs resulting from the conceptual design analysis are given in table 3.1-14 and can be compared with the corresponding generator designs resulting from the parametric studies given in table 3.1-12.

3.1.5 Weight and Losses Breakdown

The weights and efficiency tabulated in table 3.1-2 are broken down into the various components or areas as listed in tables 3.1-15 and 3.1-16. As indicated by the weight data and figure 3.1-28, there does not appear to be any further reduction in specific weight (lbs/kW) in going beyond the two to three megawatt level. The reason is that for the larger rating, the speed must be reduced due to the larger rotors, thereby increasing the electrical weight. Also, the inert weight, relative to the electrical weight, remains at about one-half the electrical weight as evidenced in table 3.1-2. At the smaller ratings, the specific weight increases due to the inert weight increasing proportionately faster. Thus, multiples of two to three megawatt generators would give minimum system weight for systems requiring very large units of electrical power.

For the losses summarized in table 3.1-16, the large bearing and seal losses for the 6 and 12 MVA designs are due to the dynamic slinger seals. If viscoseals could be made practical for these relatively low speed alternators, their efficiency could be improved significantly. The largest electrical losses in table 3.1-16 are generally the depth-behind-slot or "core" losses in the armature stacks. As pointed out in reference 7, the method for calculating these is very conservative giving high calculated losses relative to actual losses in a machine. Therefore, the efficiencies shown should actually be greater than those shown.

3.2 AC MOTOR CONCEPTUAL DESIGNS

3.2.1 Identification of Conceptual Designs

A survey of the typical mission systems defined in the electrical systems definition portion of this study, section 2.3, was made to select the most representative motor ratings for four motor conceptual designs. Essentially, the survey revealed that either many smaller rating motors (less than 100 horsepower) or a few very large ratings (greater than 100 horsepower) were the best combinations depending on the particular system. Therefore, motor ratings of 5, 30, 60, and 250 horsepower were selected from the ratings studied in the AC motor parametric study⁽⁵⁾ for the four conceptual motor design ratings. They appeared the most representative of the motor ratings required for the typical mission systems. The 5, 30, and 60 horsepower designs represent the many small motor options, and the 250 horsepower design represents the large rating option.

Table 3.1-14. Conceptual Designs

Rating (MVA)	Speed (rpm)	Gen. Weight (lbs)	Eff. (%)	Gen. Weight Plus Weight Penalty (lbs)	X_d (P.U.)	Num- ber Poles	Avg. Tan gential Stress (psi)	Prelim. Pole Face Losses (watts)	Radial Gap (in)	No. of Parallel Strands Per Con- ductor	Frame Flux Density (kL/in ²)
0.4	24000	381	91.9	781	0.76	10	47961	1859	0.100	1	108
1.2	19200	1053	93.8	1903	0.98	10	63811	1368	0.150	2	108
6.0	9000	6270	93.7	9920	1.01	16	59707	16571	0.320	4	111
12.0	7200	15848	94.0	22348	0.95	20	70878	10904	0.430	4	110

Table 3.1-15. Weight Breakdown, Conceptual Design Alternators

Item	MVA Rating			
	0.4	1.2	6.0	12.0
Rotor & Stub Shafts	202	553	3809	10086
Stack Punchings	91	261	1225	2462
Stator Frame	100	249	1431	3500
Armature & Field Windings	47	116	508	873
Bus Rings, Terminals	10	38	100	414
Bore Seal, Al ₂ O ₃ Insulation	12	25	100	600
Mounting Flange & Coolant Manifolds	63	125	780	1545
Drive End Bell, Bearing & Seals	40	60	530	1380
Antidrive End Bell, Bearing & Seals	65	100	780	1900
Potassium Charge	<u>11</u>	<u>18</u>	<u>97</u>	<u>210</u>
Total (pounds)	641	1545	9360	22970
Total Specific Weight (lbs/kW _e)	1.89	1.51	1.84	2.25

Table 3.1-16. Loss Breakdown, Conceptual Design Alternators

Item	MVA Rating			
	0.4	1.2	6.0	12.0
Armature Copper Loss	7.2	20.5	92.3	130.9
Field Copper Loss	2.2	4.4	21.0	29.5
Bus Ring/Terminal Loss	0.5	1.8	1.8	1.4
Tooth Loss	3.0	5.4	16.0	32.7
Depth-Behind-Slot Loss	13.7	29.8	87.9	175.8
Pole Face Losses	0.3	0.9	2.6	3.1
Windage Loss	0.4	1.2	4.8	11.4
Bearings & Seals Loss	<u>0.5</u>	<u>1.1</u>	<u>104.5</u>	<u>242.8</u>
Total (kW)	27.8	65.1	330.9	627.6
Alternator Efficiency - (%)	92.5	94.0	93.9	94.2

Figure 3.1-28. Alternator Weight Comparison, 800°F Conceptual Designs

A coolant temperature of 800°F was selected for all four motor conceptual designs from considerations discussed in Generator Conceptual Designs, section 3.1. The AC motor parametric study⁽⁵⁾ showed that, in general, the lower temperature motors (500°F coolant) were the lightest and most efficient motors based on both system and individual component design considerations. However, the differences between the 500°F and the 800°F coolant design weight and efficiencies was sufficiently small so that other system considerations predominated in selection of coolant temperature.

The selection of the frequencies, synchronous speeds, and voltages for the four conceptual motor design ratings were based on the results of the AC motor parametric study⁽⁵⁾. Recommendations of frequencies, synchronous speeds, and voltages for each of the ratings studied at each of the three coolant temperatures considered were made as a part of the results of the motor parametric study. It was elected to adopt the parametric study recommendations for the four conceptual design ratings for the 800°F coolant condition. This permitted the four conceptual motor design basic electrical characteristics to be identified.

The basic electrical design characteristics of the four conceptual motor designs are shown in table 3.2-1, and the 5, 30, 60, and 250 horsepower designs are labeled designs A, B, C, and D respectively to facilitate identification and reference in subsequent discussion.

The parametric study speed recommendations were based on considerations limited to electric motor design because of the lack of centrifugal circulating pump parametric data applicable to the typical power plant systems defined in section 2.3. If the choice of motor speed were limited by maximum permissible pump speed, then the maximum permissible speed, highest frequency-pole combination for the large ratings (greater than 100 horsepower) and the maximum permissible speed, lowest frequency-pole combination for the small ratings (less than 100 horsepower) would be the best alternatives. Generally, the result of reduced design speed is heavier and less efficient motors, other design conditions remaining the same.

3.2.2 Description of Conceptual Designs

Four conceptual designs were made from the basic electrical conceptual designs A, B, C, and D identified in table 3.2-1. These drawings are shown in figures 3.2-1, 3.2-2, 3.2-3, and 3.2-4, respectively. Approximate overall dimensions are indicated on the drawings. Design concepts for these designs were largely taken from previous work performed on the SNAP 50/SPUR Pump Motor Development Program(33). In that study, a reference design was made for 14 Hp, 400 Hz, 12,000 RPM, 120 Volt L-N, canned, AC induction motors as prime movers for liquid metal, centrifugal circulating pumps for the SNAP 50/SPUR nuclear electric power system. Coolants for the reference design were liquid alkali metals at a nominal temperature of 600°F. The reference design was sufficiently developed to serve as a basis for detailed

Table 3.2-1. Identification of Motor Conceptual Designs

Design	A	B	C	D
Rating (Hp)	5	30	60	250
Coolant Temperature (°F)	800	800	800	800
Frequency (Hz)	400	800	800	600
Synchronous Speed (RPM)	24000	24000	24000	12000
Voltage (L-N)	120	120	120	120

LEGEND FOR FIGURES 3.2-1, 3.2-2, 3.2-3, and 3.2-4

ITEM	COMPONENT	ITEM	COMPONENT
1	Shaft	8	Stator Ceramic Bore Seal (or Bore Barrier)
2	Rotor Laminated Stack	9	Stator Winding End Turns (or End Extensions)
3	Rotor Resistance End Ring	10	Stator Laminated Stack
4	Rotor Can (Cladding)	11	Stator Winding Connections and Bus Rings
5	Rotor Can End Plates	12	Stator Frame
6	Non Contacting, Hydro- dynamic, Screw-Type, Liquid Shaft Seals	13	Stator Heat Exchanger Jacket
7	Sleeve Type, Pivoted Pad Journal Bearings	14	Terminals

Figure 3.2-1. AC Induction Motor, 5 HP

Blank Page

Figure 3.2-2. AC Induction Motor, 30 HP

Blank Page

Figure 3.2-3. AC Induction Motor, 60 HP

Blank Page

Figure 3.2-4. AC Induction Motor, 250 HP

Blank Page

manufacturing drawings for prototype units. Subsequent technology development has indicated that essentially the same design concepts of the SNAP-50/SPUR pump motor reference design remain applicable to present pump motor design requirements. Exceptions include

- a) Sleeve-type, tilting pad journal bearings instead of plain cylindrical journal bearings used in the SNAP-50/SPUR reference design.
- b) Non-contacting, screw-type, liquid shaft seals instead of dynamic, vaned slinger seals.
- c) Elimination of an intermediate sleeve between the rotor laminated stack and the shaft.

The basic motor components are identical for all four conceptual designs and are identified in figures 3.2-1, 3.2-2, 3.2-3 and 3.2-4.

The basic design concept utilizes a three-phase, wye-connected stator winding and a squirrel cage rotor. The stator and rotor electromagnetic components are protected from the effects of contact with alkali metal in the liquid or vapor phases by hermetically sealed assemblies which incorporate a ceramic bore seal for the stator and a refractory metal cladding system around the rotor.

The stator assembly consists of a primary winding and a laminated magnetic stack supported in a hermetically sealed cavity formed by the stator frame, end bells, bore seal, and bore seal end extensions. The heat exchanger jacket (or outer half of the motor frame) and the motor stator frame jointly form the stator heat exchanger.

The primary windings (stator conductors) are electrically insulated with high temperature resistant inorganic insulation. The stator laminations are electrically insulated by interlaminar insulation to reduce eddy-current losses.

The bore seal, with its end extensions, forms the final sealing element for the stator assembly. The bore seal itself is made from a refractory ceramic composition for high electrical resistance and for alkali metal compatibility. If stator cooling and insulation design requirements (discussed in more detail in subsequent paragraphs) would permit venting the stator cavity to space vacuum, the hermetic seal requirement is eliminated and the function of the ceramic bore seal reduces to that of a barrier to prevent direct exposure of the stator electromagnetic components to alkali metal vapors.

The rotor assembly consists of the electromagnetic components (laminations, conductor bars, and end resistance rings) surrounded by a cladding system to form a hermetically sealed cavity. The cladding system, which includes the rotor shaft, end plates and rotor can, is required to protect the rotor electromagnetic components from the alkali metal vapor in the rotor

cavity. The electrical losses of a metal "can" in the magnetic air gap will not be detrimental provided contact is maintained with the rotor laminations. The rotor conductor bars and resistance end rings require high mechanical strength to withstand the rotational stresses at elevated temperature.

The journal bearings are the self-acting (hydrodynamic) tilting pad type. Self-acting (hydrodynamic) bearings are preferred for space power units because they do not require a flow of high pressure liquid potassium. The tilting pad bearing was selected from possible hydrodynamic bearing alternatives because of its inherent stability, self-alignment, and superior fluid film stiffness and damping. Furthermore, there is more successful high speed, liquid metal experience with these bearings than with any other self-acting bearings.* The bearings for the conceptual motor designs were sized by assuming a length-to-diameter (L/D) ratio of 0.5 and a bearing load capacity of 10 psi.

Thrust bearings were omitted from the conceptual motor designs because the thrust loads from the centrifugal pump impellers were unknown and the mechanical interface design between the motor and pump components was not defined. When this information becomes available, the thrust bearing may be incorporated between the drive-end journal bearing and the pump impellers (assuming a primary and a secondary pump impeller similar to the SNAP-50/SPUR pump designs).

The seals are non-contacting, hydrodynamic, screw type liquid shaft seals (viscoseals). The seals must exclude the high temperature liquid metal bearing lubricant from the rotor cavity. A liquid-vapor interface is established within the seal so that the rotor cavity is filled with the bearing lubricant saturated vapor at a pressure equal to the saturated vapor pressure of the lubricant.

The viscoseals for the conceptual motor designs utilize a double groove thread, i.e., helical grooves exist on the rotating and stationary seal members. These seal lengths were determined so that complete sealing occurs at one-half design speed with 100 percent liquid engagement.

Viscoseals are the preferred choice of seals because of their inherently lower power consumption at high speeds⁽⁶⁾ and because of their relative insensitivity to axial differential thermal expansion and other axial relative motion between rotating and stationary parts. If maximum permissible pump speeds limit the pump-motor package to lower speeds, vaned, dynamic, slinger-type seals might be considered as an alternative seal. At speeds where the slinger power consumption would not be excessive, some reduction in overall motor length might be realized

*For a more comprehensive discussion of design aspects of these bearings see the topical report on the inductor alternator parametric study⁽⁶⁾.

The shapes of the stator wire and rotor conductor bar and the stator slot configuration are different than those found in conventional motor design.

Rectangular wire, called strap, was chosen for the stator conductor instead of the more conventional round wire. Previous studies(34),(35) conducted by Westinghouse Aerospace Electrical Division determined that a wire-wound motor for this application would be impractical. The reasons for this were:

- a) Greater wire-wound stator resistance from less efficient slot use; i.e., inability to completely fill the available slot winding space with conductor because of the circular cross-section of the wire. The greater stator resistance results in larger stator losses.
- b) Higher resistance to heat transfer within the slot because of less equivalent thermal conductivity and less cross-sectional area available for heat transfer from the conductors to iron. This results in higher slot temperatures for a given coolant temperature.

The combination of factors above causes losses and temperatures to be of impractical proportions for the wire-wound designs.

Normally, the wire-wound stator would have an offsetting advantage. Because it is possible to use partially closed slots with a wire-wound motor, it is possible to make the teeth thinner without increasing the slot opening. Thus, the teeth can be thinned to the point where they are worked at the maximum magnetic density determined by the steel properties. More winding area can be obtained in this manner. Thinning the teeth of the strap-wound motor results in wide slot openings because a strap to fit the wider slot must be inserted through the slot opening. The wide slot opening causes the flux to bunch under the teeth and increase the effective length of the air gap. However, there are two factors associated with the present conceptual designs which reduce the significance of this wire-wound design advantage.

- a) These conceptual motor designs have long air gaps compared to normal standards because space must be allowed for the bore seal and rotor can as well as for running clearance. The long air gap tends to reduce the detrimental effect of wider slot openings required by strap designs.
- b) A slot configuration different than the conventional slot and similar to that employed in the inductor alternator is employed to partially close the slot while still using strap. This slot configuration with the stator rectangular wire is illustrated in figure 3.2-5.

Figure 3.2-5. Stator Slot Configuration

The better utilization of winding space and the better heat transfer for the strap-wound motor more than offsets the advantages of the wire-wound motor. Therefore, it was concluded that these motors should be strap wound.

Parallel sided teeth in the rotor laminations result in a trapezoidal slot shape. The rotor conductors have a trapezoidal shape to fully utilize the available conductor area. Trapezoidal conductor bars decrease the electrical resistance at high operating temperatures by providing maximum conductor cross-sectional area. This maintains high operating speed (decreases slip). The rotor slot configuration with trapezoidal bar is illustrated in figure 3.2-6.

The air gap of these motors is extremely wide by ordinary standards because of the presence of the ceramic stator bore seal and the rotor cladding in the electrical "air" gap. The wide air gap results in high exciting current and low power factor. The high temperatures cause the stator winding resistance to be higher than normal; this high resistance in combination with the high exciting current results in increased no-load copper losses.

The inorganic insulation used in the stator slots and on the stator conductors requires much more space than conventional insulation; therefore, the available stator conductor area is

Figure 3.2-6. Rotor Slot Configuration

less than in a conventional motor. For this reason, the ratio of steel area to copper area is larger than it would be in a conventional design.

The rotor cladding acts like another rotor cage and contributes some useful torque. However, ripples in the flux field caused by the slot openings generate high frequency losses in the cladding of such a magnitude that they cannot be neglected.

The motor cooling arrangement consists of separate heat sinks for the stator and rotor assemblies formed by the stator heat exchanger and the interior of the motor shaft respectively. All heat generated in the stator is removed at the stator heat exchanger by the stator coolant. This includes heat generated in the winding end extensions. Likewise, all heat generated in the rotor is assumed removed at the shaft inside diameter by the rotor coolant. In other words, for purposes of the conceptual design analysis, it was assumed that there is no net heat exchange between the stator and rotor assemblies by conduction, convection, or radiation. The nature of heat transfer within the motor based on this cooling scheme is discussed in the following paragraphs.

Stator losses occur in the conductors, the stator iron, and the stator bore seal. The major portion of the stator losses occur in the conductors. This heat is generated uniformly

throughout the conductor lengths including the end turns. Iron losses occur in both the teeth and the depth-behind-slot (DBS) of the punching. It is assumed that the iron losses do not vary from punching to punching so that heat generation in the iron is uniformly distributed axially along the stack.

The rotating electrical field cutting the stator bore seal can produce losses in the bore seal, depending on the material selected. The high electrical resistance of the bore seal material - a ceramic composition - keeps these losses to a negligible level.

The heat transfer paths within the stator are axial, circumferential, and radial as shown on figure 3.2-7. All the heat generated in the winding end turns is assumed conducted axially inward to the stack along each conductor since there is no provision for removing the heat directly to the stator frame other than by radiation. For purposes of the conceptual design analysis, the net heat exchange between the winding end extensions and the stator frame and end bells was neglected. The additional heat from the end turns causes a temperature rise in the punchings at the outer portions. Therefore, there is axial heat flow from punching to punching through the interlaminar insulation toward the middle of the stack.

Heat from the top coil in the slot flows primarily in a circumferential direction through the wire insulation and the slot liner to the teeth on either side of the coil, thence radially outward along the teeth to the DBS. The heat from the bottom coil flows both circumferentially through wire insulation and slot liner to the teeth on either side and radially outward through wire insulation and slot liner to the DBS. Heat from the teeth loss flows radially outward through the punching-frame interface and frame to the coolant.

The rotor losses occur in the conductor bars, end rings, at the tips of the punching teeth, and in the rotor can. The major portion of the rotor losses occurs in the conductor bars where heat is uniformly generated. The rotor can losses may be assumed to be distributed uniformly over the entire can. Heat transfer paths within the rotor are circumferential, radial, and axial as shown in figure 3.2-8.

Heat from the conductor flows circumferentially through an oxide coating on the conductor bar surface and through the conductor bar-rotor tooth interface to the rotor teeth. Heat from the rotor can combines with this heat to flow radially inward to the rotor coolant through the punching-shaft interface and the shaft itself. Heat from the end rings flows inward to the conductor bars or into the shaft depending on whether the end ring inside diameter contacts the shaft. This is determined by permissible geometrical proportions of the ring that yield the correct volume required by electrical design considerations.

An aspect of heat transfer of primary concern is the temperature drop or resistance to heat flow across a joint or interface.

Figure 3.2-7. Stator Heat Transfer Paths

Figure 3.2-8. Rotor Heat Transfer Paths

In passing from the stator conductors to the stator iron, heat must flow through three interfaces: (1) conductor-wire insulation interface, (2) wire insulation-slot liner interface, and (3) slot liner-tooth interface. Westinghouse Aerospace Electrical Division experience has been that temperature drop across the conductor-wire insulation interface is insignificant; however, the others are usually significant.

Interface thermal resistance is again of concern in cooling the rotor. Here there are four interfaces to be considered. These interfaces are: (1) the conductor bar-tooth interface, (2) the rotor can-punching interface, (3) the punching-shaft interface, and (4) the resistance end ring-shaft interface, if present. The punching-shaft interface is the most critical. Sufficient contact pressure can be maintained between the conductor bar and tooth to provide adequate heat transfer. Provided contact pressure is maintained between the rotor can and punchings (even as low as 40 psi), sufficient heat transfer can be maintained across the rotor can-punching interface.

Environment must be considered when evaluating heat transfer across an interface. For instance, under hard vacuum, it is difficult to determine the exact amount of heat transfer between surfaces forming a mechanical joint. It is probably valid to conclude conditions will be worse than when the joint is surrounded by some gas such as air, argon, nitrogen, etc. In this regard, it is important to know the conditions that exist within the sealed stator and rotor assemblies. There are several possibilities:

- a) Stator Cavity Open to Space Vacuum. Outgassing at elevated temperatures of slot components in the confined spaces of the stator slot may cause the pressure to build up in the voids and crevices to a value which produces minimum electrical strength and a phenomenon called electrical discharge corona occurs. Even though the outgassing products may promote heat transfer, conditions must be maintained such that corona effects are minimized and breakdown is prevented.
- b) Stator and Rotor Cavity Evacuated and Hermetically Sealed. In this instance the danger of rupture of component cladding with pressure buildup from outgassing exists unless the unit is designed to maintain a pressure differential.
- c) Preliminary Outgassing of Stator and Rotor Components, Evacuation, Backfilling with Inert Gas, and Hermetically Sealing. This introduces unknowns relative to fabrication techniques and the possibility of the problem described in reference 33 occurring cannot be

totally eliminated. It is also undesirable from a systems standpoint since the non-condensable gas could eventually permeate into the turbine condenser system.

For purposes of the conceptual design analysis, it was assumed that the stator and rotor cavity are hermetically sealed and that solutions to avoid corona pressures or excessive out-gassing pressure build-up have been found.

The materials selected for the various motor components and insulation systems are defined in table 3.2-2.

3.2.3 Performance of Conceptual Designs

Pertinent performance characteristics including a loss breakdown for conceptual motor designs A, B, C, and D are presented in table 3.2-3. Some pertinent temperature and cooling characteristics for each conceptual design are given in table 3.2-4. Finally, a weight breakdown for each conceptual design including electrical weights and total weights, is given in table 3.2-5.

3.3 POWER CONDITIONING COMPONENTS

3.3.1 Frequency Changer Conceptual Designs

A. Selection of Ratings. The typical frequency changers for the mission systems presented in the electrical system definitions (section 2.3) are:

- 20 kVA, 400 Hz Output, 120/208 Volts Output
- 25 kVA, 400 Hz Output, 120/208 Volts Output
- 53 kVA, 400 Hz Output, 120/208 Volts Output
- 125 kVA, 400 Hz Output, 120/208 Volts Output
- 630 kVA, 400 Hz Output, 240/416 Volts Output

These ratings are not single loads but are the total of several individual load power requirements. Therefore, for larger power requirements, a configuration of several lower rated frequency changers whose total capacity equals or exceeds the required power level may be used. This, plus the fact the parametric study (reference 4) reveals there is no advantage in making frequency changers in ratings above 125 kVA, eliminated selection of a high rating frequency changer for conceptual design. Comparison of the above data with the parametric study data (reference 4) reveals that, although no exact matches exist, several comparable systems are evident. The conceptual designs; therefore, were selected from the parametric study designs and additional analyses for complete new designs were not required.

Table 3.2-2. Conceptual Motor Design Component Material

Component	Material			
	Design A	Design B	Design C	Design D
1. Stator Heat Exchanger Jacket	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)
2. Stator Frame	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)
3. Stator Laminations	Hiperco 27(0.004 inch-thick laminations)	Hiperco 27(0.004 inch-thick laminations)	Hiperco 27(0.004 inch-thick laminations)	Hiperco 27(0.004 inch-thick laminations)
4. Stator Interlaminar Insulation	Plasma-Arc Sprayed Alumina	Plasma-Arc Sprayed Alumina	Plasma-Arc Sprayed Alumina	Plasma-Arc Sprayed Alumina
5. Stator Conductor	Inconel 600 Clad Silver	Inconel 600 Clad Silver	Inconel 600 Clad Silver	Inconel 600 Clad Silver
6. Stator Conductor Insulation	Plasma-Arc Sprayed Alumina	Plasma-Arc Sprayed Alumina	Plasma-Arc Sprayed Alumina	Plasma-Arc Sprayed Alumina
7. Stator Slot and Phase Insulation (including wedges)	Alumina, 98.5% Purity	Alumina, 98.5% Purity	Alumina, 98.5% Purity	Alumina, 98.5% Purity
8. Stator Bore Seal	Alumina, 99% Purity	Alumina, 99% Purity	Alumina, 99% Purity	Alumina, 99% Purity
9. Rotor Cladding (can and end plates)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)
10. Rotor Conductor Bar	DS Copper (Cube)	DS Copper (Cube)	DS Copper (Cube)	DS Copper (Cube)
11. Rotor Resistance End Ring	DS Copper (Cube)	DS Copper (Cube)	DS Copper (Cube)	DS Copper (Cube)
12. Rotor Laminations	Nivco Alloy(0.014 inch-thick laminations)	Nivco Alloy(0.014 inch-thick laminations)	Nivco Alloy(0.014 inch-thick laminations)	Nivco Alloy(0.014 inch-thick laminations)
13. Rotor Shaft	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)
14. Seals (Viscoseals)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)
15. Terminal Conductors	Inconel 600 Clad Silver	Inconel 600 Clad Silver	Inconel 600 Clad Silver	Inconel 600 Clad Silver
16. End Bells	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)	Tantalum T-111 Cb-1Zr(a)
(a) Alternate material				

Table 3.2-3. Conceptual Motor Design Performance

	Design A	Design B	Design C	Design D
Efficiency (Percent)	72.5	80.9	83.4	89.1
Power Factor	0.776	0.583	0.740	0.727
Phase Current (rms) (amps)	18.4	131.8	201.7	799
Rated Torque (lb-ft)	1.1	6.71	13.3	111.4
Maximum Torque (lb-ft)	2.43	16.2	29.9	273.1
Locked Rotor Torque (lb-ft)	0.587	4.87	5.2	60.5
Locked Rotor Phase Current (rms) (amps)	84.9	473.4	855	3474
Slip (Percent)	1.27	2.2	1.15	1.74
Losses (watts)				
Stator				
I^2R	1139	3387	6524	14916
Iron	44	350	363	1038
Rotor (I^2R)	50	518	537	3343
Stray Load	9.9	234	173.8	670
Surface	118	685	984	1935
Bore Seal	<0.0001	<0.001	<0.001	<0.01
Rotor Can	49.9	85	260	315
Bearings and Seals	<0.01	2	10	425
Windage	8	24	71	75
Total	1418.8	5285	8922	22717

Table 3.2-4. Conceptual Motor Design, Critical Temperatures and Coolant Requirements

Stator and Rotor Coolant: Liquid Potassium at 800°F (nominal)				
Maximum Coolant Velocity = 50 feet/second	Design A	Design B	Design C	Design D
Average Stator Winding Temperature (°F)	1100	1175	1230	1150
Stator Hot Spot Temperature(Stator End Extensions) (°F)	1200	1250	1310	1290
Average Rotor Conductor Bar Temperature(°F)	1050	925	950	1010
Average Rotor Resistance End Ring Temperature(°F)	1060	935	960	1050
Coolant Flow Rate (lb/sec)				
Stator	1.0	1.0	1.0	1.5
Rotor	0.2	0.2	1.0	1.0
Coolant Temperature Rise(°F)				
Stator	10	18	34	72
Rotor	10	15	10	17

Table 3.2-5. Conceptual Motor Design Weight Breakdown

	Design A	Design B	Design C	Design D
Stator Electrical Weight (lbs)				
Winding	1.6	3.6	6.2	30.6
Iron	6.0	16.0	16.6	74.5
Total Stator Electrical Weight(lbs)	7.6	19.6	22.8	105.1
Rotor Electrical Weight (lbs)	4.1	6.6	14.2	47.3
Total Motor Electrical Weight (lbs)	11.7	26.2	37.0	152.4
Heat Exchanger Jacket (lbs)				
T-111	9.2	12.2	15.4	31.7
Cb-1Zr	5.0	6.6	8.3	17.1
Stator Frame (lbs)				
T-111	4.7	9.1	11.7	54.4
Cb-1Zr	2.5	4.9	6.3	29.4
Bore Seal (lbs)	0.8	0.9	2.3	3.4
End Bells (lbs)				
T-111	11.8	17.0	16.7	39.4
Cb-1Zr	6.4	9.2	9.0	21.3
Shaft (lbs)				
T-111	1.9	2.1	3.4	35.1
Cb-1Zr	1.0	1.1	1.8	19.0
Rotor Cladding(can and End Plates) (lbs)				
T-111	1.1	1.7	3.0	9.1
Cb-1Zr	0.6	0.9	1.6	4.9
Bearings	0.5	0.5	1.9	7.9
Seals				
T-111	1.2	1.2	1.3	5.1
Cb-1Zr	0.7	0.6	0.7	2.7
Miscellaneous (lbs)	3.0	3.0	3.0	5.0
Total Motor Weight (lbs)				
T-111	45.9	73.9	95.7	343.5
Cb-1Zr	32.2	53.9	71.9	263.1
Electrical Specific Weight(lbs/hp)	2.3	0.87	0.62	0.61
Overall Specific Weight(lbs/hp)				
T-111	9.2	2.5	1.6	1.4
Cb-1Zr	6.4	1.8	1.2	1.1

The system input frequencies were assumed as they were not specified in the electrical system definitions and the alternator conceptual designs had not been established at the time the frequency changer designs were selected. The frequencies selected were cognizant of the most likely alternator frequencies as given in reference 3.

A major user of the frequency changers is the ac pump motors. Summary data of reference 5 indicates that a changer output frequency and voltage of 400 Hz and 120/208 volts is compatible with the most likely motor requirements.

B. Frequency Changer Description. The design and performance parameters of the frequency changers are presented in table 3.3-1. All the conceptual designs make use of silicon thyristors rather than high-temperature gas tubes. Their selection was based on weight and availability. The weights of the gas tube units are much greater and are still in the development stage whereas the silicon thyristors are available in production quantities. The cycloconverter approach, without input or output transformers, was used in all designs. The basic cycloconverter power circuit consists of a pair of half-wave, thyristor bridges connected in reverse parallel for each output phase. This configuration is used for all conceptual designs except the 5-kVA rating unit. For the 5-kVA rating unit, it is necessary to operate thyristors in series (two) to obtain the required voltage. The detailed electrical and mechanical design concepts are presented in reference 4. The total weights shown in table 3.3-1. are from more detailed calculations than were made for the parametric study.

The packaging arrangements for the conceptual designs are presented in figures 3.3-1 and 3.3-2. Silicon thyristors are normally supplied with a hex base that has a threaded stud protruding from the bottom. The stud is used for mounting the thyristor and making the anode connection. For thyristor mounting by adhesive bonding, a modification of the base was required. A rectangular base was designed that provides a large flat surface for adhesive bonding and an anode connection location on the top. The new base configuration shown in figure 3.3-3 results in a simpler system of mounting, conduction cooling and electrical isolation than would be required for stud-mounted thyristors.

The control components are not shown in figure 3.3-1 and 3.3-2 since actual design of these components was not established. The volume of the control components was estimated for use in determining package size requirements for the complete units.

It is apparent from table 3.3-1 and figure 3.3-2 that, from a physical size standpoint, the 25, 50, and 100-kVA units are identical. This similarity results from the use of the same quantities of similar components in all three designs. This situation arises since only existing components were used in the designs and many of these components, although having different electrical characteristics, are physically similar.

Table 3.3-1. Cycloconverter Conceptual Design Summary, 200 Volt,
400 Hz Output, Power Factor 0.7

Rating (kVA)	5	25	50	100
Input Frequency (Hz)	2000	2000	1200	2000
Thyristor	GE C11E	(W) 219Z	(W) 219Z	(W) 254
No. in Series	2	1	1	1
No. in Parallel	1	1	1	1
Max. Temperature (°F) (Case)	194	212	194	180
Thyristor Losses (watts)	3.31	21.3	45.2	78.3
Conduction and Switching Losses	2.81	18.3	42.2	75.3
Gate Loss	0.5	3	3	3
Inductance, L_c (μ -Henry)	167	33.4	30.1	8.35
Coil Temperature (°F)	284	284	284	284
Loss (watts)	2.08	6.94	18.2	19.6
Weight (pounds)	0.074	0.248	0.647	0.7
Dimension "a" (inches) (a)	0.303	0.454	0.626	0.642
Control Loss (watts)	48.5	69	74	69
Control Weight (pounds)	4.4	6.4	7	6.4
Total Losses (watts) (b)	187	515	1051	1655
Input Current RMS (amp)	13.4	67.1	134	268
Output Current RMS (amp)	11.1	55.6	111	222
Inductor Current RMS (amp)	6.42	32.1	64.2	128.5
Thyristor Current RMS (amp)	4.54	22.7	45.4	90.8
Thyristor Current Average (amp)	1.67	8.3	16.7	33.4
V_{FW} (Thyristor)	2.8	1.6	2.0	1.27
V_{CO} (Commutating)	10	10	10	10
V_C (Inductor)	8.05	8.05	13.3	8.05
$0.828 (E_s - V_{CO} - V_C)$	150.7	150.7	146	150.7
V_O (Load)	147.9	149.1	144	149.4
Output (kVA)	4.93	24.85	48	99.5
Efficiency (%)	94.6	97.1	97.0	97.7
Total Weight (lb)	23.4	36.3	38.1	37.5
Package Volume (in ³)	891	1041	1041	1041
Coolant	MIPB	MIPB	MIPB	MIPB
Coolant Ave. Temp (°F)	181	181	140	92
Coolant Flow Rate (lb/min)	1.16	3.18	6.69	11.18
(a) See Figure 2-5. of Reference 4.				
(b) Total Loss = Control Loss + 9 (No. Parallel) [2(No. Series) (Thy. Loss) + Ind. Loss]

Figure 3.3-1. Semiconductor Cycloconverter, 5 kVA

Blank Page

Figure 3.3-2. Semiconductor Cycloconverter, 25, 50, and 100 kVA

Blank Page

Figure 3.3-3. Thyristor Configuration for Frequency Changer
Conceptual Design

3.3.2 Rectifier Conceptual Designs

A. Selection of Ratings. The typical rectifiers for mission systems presented in the electrical systems definitions (section 2.3) have ratings ranging from 3 to 550 kVA and voltages of 28 to 30,000 volts. Based on these data, three conceptual designs of 25 kW, 30V, 75 kW, 120 V and 500 kW, 20,000 V were selected as most representative. A fourth design 250 kW, 5000 V was selected to represent designs between the 75 and 500 kW designs. Complete new rectifier designs were made for the 25 and 500 kW units since their rating and voltage combinations were not included in the previous parametric study. The 75 and 250 kW designs were selected from the parametric study data.

The fact that input frequencies were not specified in the electrical systems definitions is of little consequence since, in the range considered (400 to 3200 Hz), frequency has little effect on the total weight or losses of the rectifiers (reference 4).

B. Rectifier Descriptions. The design and performance parameters of the rectifiers are presented in table 3.3-2. Only silicon diodes were used in these designs since the gas and vapor tubes investigated in the parametric study (reference 4) are still in the development stage. In the design of the 250 and 500-kW systems, it is necessary to use diodes in series to obtain the required voltage capabilities. Voltage division across series connected diodes is accomplished by shunting each diode with a resistor-capacitor network. These arrangements are required since only available components were used and individual silicon diode voltage ratings are not necessarily compatible with specifications of the systems. The low voltage requirements of the 25 and 75-kW systems do not require series connection of diodes. The detailed electrical and mechanical design approach used for the rectifier systems is presented in the parametric study discussions of reference 4. The total system weights shown in table 3.3-2 are the result of a more detailed calculation than that used for the parametric study.

The packaging arrangements for the conceptual designs are shown in figures 3.3-4 and 3.3-5. The small number of components of the 25 and 75-kW systems (figure 3.3-4) are arranged on a single cold plate which results in relatively small packages. In the 250 and 500-kW design, where each diode is shunted with a resistor and capacitor, these components are grouped together to facilitate wiring. For the 250-kW design (figure 3.3-5), the diode, resistor, and capacitor groupings are arranged 24 to each of three cold plates (two rectifier circuit legs per cold plate). The cold plates are in a stacked arrangement. The groupings of diode, resistor, and capacitor for the 500-kW design are arranged 48 to each of six cold plates (one rectifier circuit leg per cold plate). The cold plates of this design are also packaged in a stacked arrangement.

The silicon diodes used in these designs are normally supplied with a hex base and a threaded stud protruding from the bottom. The silicon diode bases shown in figures 3.3-4 and 3.3-5 are a modification to allow mounting by adhesive bonding. This configuration is as described for the silicon thyristors used in the frequency changers and shown in figure 3.3-3.

The effect of using only available components is evident as shown in table 3.3-2 in that the weight and flow rate for the 75-kW system are lower than for the 25-kW system. This situation is due to the different characteristics of diodes used in rectifier designs that result in similar configurations. The situation was not unexpected as similar results appeared in the parametric study.

Table 3.3-2. Rectifier Conceptual Design Summary

	400 to 3200	400 to 3200	400 to 3200	400 to 3200
AC System Frequency	25	75	250	500
DC Bus Power (kW)	30	120	5000	20000
DC Voltage (volts)	833.3	625	50	25
DC Bus Current (amps)	Six-Phase, Parallel, Single-Way	Six-Phase, Parallel, Single-Way	Six-Phase, Double-Way	Six-Phase, Double-Way
Rectifier Circuit	1	1	1	1
Qty. Rectifier Assemblies	JEDEC	JEDEC		
Diode:				
Type	IN4588	IN4593	(W) 402Z	(W) 402Z
Rect. Total Current (amps)	833.3	625	50	25
Peak Amps/Rect. Leg	208	156.25	50	25
Avg. Amps/Rect. Leg	69.4	52.1	16.7	8.33
PIV (volts)	200	800	1000	1000
Series Diodes/Rect. Leg	1	1	12	48
Parallel Diodes/Rect. Leg	None	None	None	None
Total Qty. Diodes	12	12	72	288
Total Losses (watts)	960	777	1190	2477
Total Weight (lbs)	3.6	3	4.5	18
Resistor:				
Type	None	None	Dale RS-10-39	Dale RS-10-39
Resistance (k-ohms)			200	200
Total Qty. Resistors			72	288
Total Losses (watts)			27.7	111.17
Total Weight (lbs)			1.43	5.7
Capacitor:				
Type	None	None	Tubular	Tubular
Capacitance (μfd)			Bendix E-200	Bendix E-200
Total Qty. Capacitors			0.01	0.01
Total Losses Max. (watts)			72	288
Total Weight (lbs)			10.9	74.6
			1.36	5.44
Total Component Weight (lbs)	3.6	3	7.29	29.14
Total Losses Max. (watts)	960	777	1228.6	2662.77
Rect. Conversion Eff. (%)	96.3	98.97	99.51	99.47
Total System Weight (lbs)	12.65	11.84	38.5	138.5
Package Volume (in ³)	373	324	1831	7053
Coolant	MIPB	MIPB	MIPB	MIPB
Coolant Avg. Temp. (°F)	168	156	201	217
Coolant Flow Rate (lbs/min)	6.1	5	7.6	16

3.3.3 Transformer Conceptual Designs

A. Selection of Ratings. The primary requirement for transformers for mission systems presented in the electrical system definition (section 2.3) are for the three phase type; therefore, single phase transformers were not considered for conceptual design. Transformer conceptual design selection was based on the electrical output parameters of the generator conceptual designs. This approach was taken to be consistent with electrical system definitions which indicate the transformers may operate directly from generator output. As a result, new designs had to be calculated since the voltages and frequencies desired for the conceptual designs did not coincide with any designs included in the previous parametric study (reference 4).

B. Transformer Description. The design and performance parameters of the transformer conceptual designs are presented in table 3.3-3. These designs were calculated using the design procedures used in the parametric study. These design concepts are reported in detail in reference 4. An average coolant temperature of 800°F was selected for the conceptual designs so as to be compatible with the generator heat rejection system since both the transformer and generator use potassium as the coolant. For the selected parameters of rating, frequency, voltages, and coolant average temperature, transformer designs were calculated for varying operating temperatures and number of cooling ducts in the coil. From these data, curves of total system weight versus efficiency are obtained (figure 3.3-6). The optimum transformer designs, therefore, correspond to the minimum weight points of these curves. The total system weight is the sum of the transformer weight and system weight penalty. The system weight penalty consists of two parts, that of the electrical power system supplying make-up power to account for the losses and that of the cooling system to reject these losses. The weights listed in table 3.3-3 are the sum of the transformer electrical weight and its integral cooling apparatus. All transformer designs were made using strip conductor in the windings and four-mil tape-wound material with an inorganic binder for the magnetic core. The transformer and integral cooling system configuration are shown in figure 3.3-7.

For all conceptual designs, only four coil cooling ducts (two per side) were required and the maximum operating temperatures are below the maximum safe operating temperatures of the materials. The secondary windings are the location of the maximum transformer temperature in all designs. Figure 3.3-8 shows the expected trends of increasing transformer weight, efficiency and flow rate with increasing kVA rating.

Figure 3.3-4. Rectifier Layout, 25 and 75 kW

Blank Page

Figure 3.3-5. Rectifier Layout, 250 and 500 kW

Blank Page

Table 3.3-3. Three Phase Transformer Conceptual Design Summary
(800°F Average Coolant Temperature)

Rating (kVA)	250	500	5000	10000
Frequency (Hz)	2000	1600	1200	1200
Primary Voltage, L-N (volts)	500	500	500	500
Secondary Voltage, L-N (volts)	2140	857	1000	1000
Primary Current (amps)	166.7	333.3	3333.3	6666.7
Secondary Current (amps)	39	194.5	1666.7	3333.3
Maximum Operating Temp. (°F)	975	974	1178	1283
Total Losses (watts)	2406	5085	33957	54794
Power Factor	0.95	0.95	0.95	0.95
Efficiency (percent)	99.05	98.99	99.33	99.46
Size -				
Length (inch)	11.8	12.5	22.56	27.86
Width (inch)	5.5	6.23	11.46	14.35
Height (inch)	7.86	9.35	15	18.57
Transformer Weight (lbs)	63.7	96.93	662.1	1226.6
Coolant	Potas- sium	Potas- sium	Potas- sium	Potas- sium
Coolant Flow Rate (lb/min.)	7.6	16.1	107.3	173.1
No. of Cooling Ducts in coil/side	2	2	2	2

Figure 3.3-6. Three-Phase Transformer Conceptual Design Data

Rating (kVA)	Length (in.)	Width (in.)	Height (in.)	"A" Dim (in.)	"B" Dim (in.)	"C" Dim (in.)
250	11.7975	5.495	7.864	2.75	5.364	9.0525
500	12.488	6.225	9.350	3.625	5.074	9.888
5000	22.562	11.458	15.041	7.125	8.541	18.228
10000	27.856	14.348	18.572	9.000	10.572	22.508

EDSK 342881

Figure 3.3-7. Transformer Conceptual Design Layouts

Blank Page

Figure 3.3-8. Three-Phase Transformer Conceptual Design Data

Blank Page

4. CONCLUSIONS

Because of the many possible tradeoffs between power system operating conditions and mission loads, it is almost impossible to arrive at one set of system parameters giving minimum weight. However, it is comforting to note there are several compatibilities between the various minimum weight component configurations. For instance, the desirable frequencies, relative and absolute, and voltages of the alternators, ac induction motors, and cycloconverters are strikingly compatible and result in near "natural" configurations. Likewise, the high frequencies and low voltages of the alternators are compatible with the desired inputs for the transformers; however, the low alternator voltages do show incompatibilities with the desirable higher voltages for minimum weight contactors and distribution systems.

The choice of the higher alternator frequency on the basis of the alternator requirements alone appears desirable from the total system standpoint too since, in these large ratings, as much as 80 to 90 percent of the user loads require dc power and the desirable frequencies for the major ac loads are lower, the reduction being about 3:1 as referred by the frequency changer⁽⁴⁾. While future insulation developments to increase voltage capabilities to kilovolt levels should allow alternator designs more compatible with desired distribution system voltages, the added complexities of series winding rigid or glass insulation systems in the alternator may preclude their use. The speeds of these large rating alternators are sufficiently high in the 500° to 800°F coolant designs to allow efficient, minimum weight turboalternators to be produced. Above 800°F, the limiting strengths of the available magnetic rotor materials results in lower speed operation, heavier alternators and most likely, compromises in the desirable turbine speeds. As a result, little appears to be gained at this time in operating the alternators at very high coolant temperatures for the sake of minimizing auxiliary cooling systems and radiator weights. It appears that significant improvements in the temperature capabilities of the large rating alternators can best be served by developing higher strengths in the high-temperature magnetic rotor alloys.

The desirable frequencies and voltages of the ac pump motors appear compatible with the systems as a whole. The desired synchronous speeds appear high from the standpoint of the centrifugal pumps and some design tradeoffs may be required.

For the cycloconverters, silicon-thyristors are the best approach at present because of the lower weight and better availability. The gas tube thyristors appear to be more applicable to the higher voltages that are inapplicable to cycloconverters. Efficiency varies little with the other parameters except at low power factor on low power rating units. Little is to be gained by building cycloconverter modules above ratings of 125 kVA or

with output frequencies much above 400 Hz. Higher ratings can be obtained by paralleling modules. Also, a relatively low voltage power source is desirable. It appears that significant improvements in high power cycloconverter design can best be served by rapid advancements in the switching component state-of-the-art.

The choice of rectifier operating parameters in a space nuclear power system can only be made after considering all the system parameters such as source voltage, transmission requirements, and load voltage flexibility. However, the development of silicon diodes of much greater voltage capability will reduce the number needed in series for high voltages and thereby decrease weight, not only by elimination of diodes but also by removing the associated voltage division networks.

As stated previously, transformer input voltage variations have little effect between 500 to 2140 volts L-N. Frequency should be relatively high although 3200 Hz shows little advantage over 2000 Hz. Output voltages should preferably be below 2000 volts although in general this will be governed by the loads. Although the weight advantages of higher coolant temperatures may be only moderate in the transformer, it appears reasonable to assume that reductions in cooling system requirements would justify concentrating future developments in the high temperature area.

The technology to meet transmission line requirements for these systems appears to be readily available. Required contactors are not available however and present development efforts should be continued and expanded, especially in the areas of higher current capability.

From the standpoint of overall mission load requirements and potential for component development, an electrical component development program aimed at electrical power systems in the one megawatt range appears likely to yield the most applicable technology for future years or missions to come. When and if, in the far distant future, tens of megawatts of space electric power are needed, the power systems could be built on paralleled megawatt systems.

5. REFERENCES

1. Quarterly Technical Progress Report No. 1, SAN 679-1, Sept. 1 to Nov. 30, 1966.
2. Quarterly Technical Progress Report No. 2, SAN 679-2, Dec. 1, 1966 to Feb. 28, 1967.
3. Quarterly Technical Progress Report No. 3, SAN 679-3, March 1, to May 31, 1967.
4. Topical Report, Power Conditioning Parametric Screening Study, SAN 679-4, September, 1967.
5. Topical Report, Parametric Design Study of Canned AC Induction Motors, SAN 679-5, January, 1968.
6. Topical Report, Parametric Study of Inductor Alternators, SAN 679-6, February, 1968.
7. Topical Report, Experimental Verification, SAN 679-7, February, 1968.
8. Private Communication from AEC to Westinghouse dated September 15, 1966.
9. Research on Spacecraft and Power Plant Integration Problems, Contract NAS 3-2533, General Electric, Missile and Space Division.

First Quarterly Report	GE63SD760	7/26/63
Second Quarterly Report	GE63SD886	10/26/63
Third & Fourth Quarterly Reports	GE64SD700	4/26/64
Spacecraft Topical Report	GE64SD892	7/24/64
10. Study of Low-Acceleration Space Transportation Systems, Phase II Study Report, Contract NAS 8-11423, Vol. I, Summary Report No. GE66SD2026, General Electric, Missile and Space Division, July 15, 1966.
11. Manned Orbital Space Station Power Conversion and Control, Report No. SID-63-36-6, Westinghouse Electric, Aerospace Electrical Division, February 1, 1963.
12. Study for an On-Board Electrical Power System for a Manned Orbital Space Station, Contract NAS 9-1307, Lockheed Aircraft Co., November 24, 1963.

13. Mission and Power Subsystems Studies, Vol. II, General Electric, Missile and Space Division, Report No. AFAPL-TR-65-56, July, 1965.
14. Nuclear Power Plant, Lunar Exploration Systems for Apollo, Contract DA-49-129-ENG (NASA)-1A, Westinghouse Electric, Astronuclear Laboratory Report No. WANL-PR(S)-009A, October 1, 1964.
15. Engineering Study of Multi-Purpose Engine and Fuel System, Lunar Exploration System for Apollo, Contract DA-49-129-ENG (NASA)-1B, Westinghouse Electric, Astronuclear Laboratory Report No. WANL-PR(S)-006B, Volumes I-IV, May 1, 1964.
16. Electromagnetic Alkali Metal Pump Research Program, Contract NAS 3-2543, General Electric Co., Report No. NASA CR-380, February, 1966.
17. "Power Conditioning Requirements for Ion Rockets" by J.T. Kotnik and B.L. Sater; IEEE Transactions on Aerospace, Vol. 2, No. 2, April, 1964.
18. Guidance for Aircraft Electrical Power Utilization and Transient Protection, Aeronautical Radio Inc., Committee Project Paper No. 413 of the Air Lines Communications Administrative Council, September 22, 1964.
19. "SNAP 8 Reactor and Shield Designs & Operating Experience" by C. E. Johnson & D. G. Mason; Jnl. of Spacecraft, Vol. 3, No. 7, July, 1966.
20. Space Powerplant Study, Contract NAS 5-250, Westinghouse Electric, Astronuclear Labs, Report WANL-PR-(B)-009, December, 1963.
21. Advanced Nuclear Electric Power Generator System Study, Rankine Cycle Nuclear Space Powerplant, Pratt and Whitney Aircraft Co., Report No. PWA-2232, August, 1963.
22. SNAP-50 Optimization Studies, Pratt & Whitney Aircraft Report PWAC-391, March 1, 1963.
23. SNAP-50/SPUR First Flight Power Plant Preliminary Design Specification, P & W AC Report CNLM-5118, June 21, 1963.
24. SNAP-50/SPUR Powerplant Specifications, P & W AC Report CNLM-4400, Supplement 6, June 1, 1964.
25. SNAP-50/SPUR Powerplant Requirements Report, P & W AC Reports CNLM-6291 Through 6297, June 1965.

26. Work on Contract NAS 5-1234 was reported in six volumes:

Vol. I, II, III, and IV as Westinghouse Aerospace Electrical Division Report No. D-709533, December 1962. Volume IV numbered NASA-CR-50861.

One volume as Westinghouse Aerospace Electrical Division Report No. WAED 63.11E. December 1963.

One volume, unnumbered, by Westinghouse Aerospace Electrical Division on 250 and 500 kW designs on an extension of NAS 5-1234.

Official NASA contractor report numbers were not applied to these reports except volume IV.

27. SNAP 50/SPUR 467 kVA Experimental Generator Design Summary, USAF Systems Command Report AFAPL-TR-67-7, May, 1967. Prepared by Westinghouse Aerospace Electrical Division.
28. SPUR Generator Stator Seal Development, Topical Report, USAF Systems Command Report ASD-TDR-63-677; Part I, August 1963; Part II, February 1967. Prepared by Westinghouse Aerospace Electrical Division.
29. Alkali Metal Resistant Wire, USAF Systems Command Report, APL-TDR-66-42, April 1964. Prepared by Westinghouse Aerospace Electrical Division.
30. High Temperature Alkali-Metal Resistant Insulation, USAF Systems Command Report AFAPL-TR-65-22, March, 1965. Prepared by Westinghouse Aerospace Electrical Division.
31. AC Generator Rotor Materials Topical Report, USAF Systems Command Report AFAPL-TR-66-127, January 1967. Prepared by Westinghouse Aerospace Electrical Division.
32. Magnetic Materials Topical Report, NASA Lewis Research Center Report, NASA-CR-54091, September, 1964. Prepared by Westinghouse Aerospace Electrical Division.
33. Reference Motor Design, SNAP-50/SPUR Pump Motor Development Program, Pratt & Whitney Aircraft Division - CANEL Report No. 469, PWAC Contract AT(30-1)2789 prepared by Westinghouse Aerospace Electrical Division under agreement 65-9, Sept. 3, 1965.
34. Final Report, Task 2, SNAP-50 Pump Motors Design Study, Contract No. AEC (30-1)2789, Task 2, Westinghouse Aerospace Electrical Division Report No. 63.15, December 10, 1963.
35. Supplement to Final Report, Task 2, SNAP-50 Pump Motors Design Study, Contract No. AEC (30-1)2789, Task 2, Westinghouse Aerospace Electrical Division Report No. 63.15, December 10, 1963.

Blank Page

APPENDIXES

Blank Page

APPENDIX A

GENERATOR AND MOTOR STATOR BORE-SEALS AND ROTOR CANS

Generator and motor windings fabricated from present state-of-the-art materials must be protected from attack by alkali-metal coolants if their metal vapors are present in the rotor cavity. Rotors with windings may be protected in metal cans.

Protection of the stator from potassium vapor requires a bore-seal in the form of a thin-wall tube in the radial gap of the alternator. A thin-wall ceramic tube has been judged best suited to provide a good combination of resistance to attack by alkali metal and electric resistivity high enough to limit eddy-current losses to low values. The ceramic tube in the gap between the stator and rotor poles must be bonded to metal end members for sealing to the alternator frame in order to provide a sealed stator enclosure. In the event that both the stator and rotor would be vented to space, the seal would not necessarily be hermetic.

A. Background

Several programs have been carried on for development of fabrication techniques to provide alkali-metal resistant bore-seals and cans. Developments under the programs referenced below are discussed in the next section on Program Developments.

1. Air Force Program - An Air Force program for development of a 467 KVA potassium cooled SNAP-50/SPUR type generator included a task for development of a stator bore-seal in that unit. Technical Report ASD-TDR-63-677, Part I (1963) and Part II (1967), covers this work carried out on SNAP-50/SPUR Development Contracts AF33(616)7379 and 8322, AF33(657)8954, and 10922, AF33(615)1551 and 2326.
2. NASA Program - A NASA sponsored program was carried out for study of ceramic-metal seal technology appropriate for fabrication of stator bore-seals for service at higher temperatures up to 1600°F. Research and Development Program on Magnetic, Electrical Conductor, Electrical Insulation and Bore Seal Materials, Contract NAS 3-4162. Technical Report NASA-CR-54093 (1964) covers much of the past work done under this study.

3. CANEL Program - An AEC sponsored program for development of liquid-metal pump motors in the CANEL space power system included application of motor stator bore-seals and metallic enclosure cans for the rotor.

B. Program Developments

1. Bore-Seals to 1000°F - Alumina is a widely used ceramic in commercial applications involving its being brazed to metal components in vacuum tight joints. Superior strength and better availability in special shapes were added factors which made alumina an initial first choice over beryllia for stator bore-seals to operate below 1000°F in the investigations carried on under the Air Force program for development of the SNAP 50/SPUR type generator.

Brazing trials were performed with the three types of specimens shown in Figure A-1. Type CLM-15 tensile specimens from ceramic bodies of five different alumina of high purity were used in brazing to thin (0.015 inch) metal washers of refractory and non-refractory alloys. Five different commercial brazing alloys were used in early trials by conventional processes. Further brazing was done with titanium and zirconium base alloys of the "active-metal" type produced in twelve special melts from high purity materials. The brazing trials and subsequent tensile testing and 10,000-hour exposure to potassium vapor at 900°F demonstrated alumina of 99.8 percent purity brazed under high-vacuum at about 1850°F with the "active-metal" alloy of 75Zr/19Cb/6Be to metal members of 99Cb/1Zr alloy produced the best combination of high tensile strength (7500 psi), vacuum tightness, and good brazing characteristics of those specimens which resisted potassium attack.

Exposure tests with 2.5 inch diameter tubular specimens having a uniform 0.375-inch thick wall demonstrated the materials were resistant to both potassium liquid attack (on the bottom brazement) and to vapor (on the top) at 1100°F over a 1000-hour period.

The 11.5 inch diameter brazement specimen shown at the bottom of Figure A-1 also had a uniform 0.375 inch wall thickness because that is the dimension chosen for the thicker brazed section at the ends of thin-wall tubes. Thin-Wall (0.090 inch) tubes, as shown in Figure A-2, were produced also from CE998 alumina by WESGO. Brazing trials with the large tubular bore-seal type specimens revealed that titanium base brazing alloys did not produce vacuum tight joints in these heavier assemblies which had relatively slow heating and cooling ratios.

Figure A-1. Types of Brazed Specimens in SNAP 50/SPUR Program

Figure A-2. Set of Thin-Wall (0.090-inch) Ceramic Component
For Prototype SNAP-50/SPUR Generator's Stator
Bore-Seal Assembly

Metal members of Cb-1Zr were 0.030 inch thick in these specimens because stress analyses indicated a need for that thickness.

Although most of the work carried out on the SNAP 50/SPUR generator bore-seal was of a materials development nature, extensive analytical work was done to determine and minimize the stresses in the bore seal, joint, and metal end-member. The work consisted of a computerized study of several 2-dimensional shell configurations (over fifty) subjected to all possible modes of steady state and transient conditions that the bore seal would see during fabrication, assembly and operation. As a result of this study, several configurations and design approaches were identified which would minimize stress in the bore seal and end members, thereby increasing its dependability.

2. Bore-Seals for 1000°F - 1600°F - At temperatures over 1000°F in longtime exposure to alkali-metal vapors, testing has demonstrated that alumina must be replaced by high-purity beryllia ceramics to withstand attack.

High-purity beryllia has not been available in the larger diameter tubes (over 6 inches diameter) for use as bore-seals. However, supplier capabilities have been improving in regard to fabrication of thin wall tubes of uniform high density from this ceramic.

The alloy of 60Zr/25Va/15Cb displayed the best combination of potassium resistance, strength, and good brazing characteristics from more than ten "active-metal" alloys evaluated in small modulus-bar brazed specimens. Specimens with Cb-1Zr alloy members brazed to beryllia ceramic with molybdenum surface metallizing had a strength of 25,000 psi both at 1600°F and room temperature. Their strength did not deteriorate after 500 hour exposure to potassium at 1600°F. Vapor exposure at that temperature for 2000 hours reduced the strength about 10 percent and liquid potassium caused the joint strength tests to vary inconsistently after exposure.

A test presently in progress on a 4 inch diameter bore-seal assembly of those materials at 1300°F has shown no evidence of potassium leakage after 5000 hours.

The tantalum base alloy, T-111 with 90Ta/8W/2Hf may be more compatible with assemblies now considered for future liquid-metal cooling systems from the standpoint of minimizing mass transfer. It has been found to wet well by brazing alloys in limited trials of about a dozen specimens.

Figure A-3. Thermal Expansion of Ceramic and Metal Bore-Seal Components (from NASA-CR-54093)

The expansion curves in Figure A-3 show this alloy to be slightly better than Cb-1Zr in matching alumina and beryllia. In high temperature tensile tests it has shown yield strengths of 110,000 psi at 1000°F and 80,000 at 1600°F; both are roughly three times those for the columbium base alloy.

3. Rotor Cans - A high speed motor's wound rotor needs an enclosing can for protection of its windings from alkali metal. The good mechanical properties of T-111 alloy at high temperatures and its resistance to alkali metal attack led to the choice of this material for the rotor can on CANEL pump motors. Metallic rotor cladding contributes some motor torque, but the electric resistivity of T-111 (43×10^{-6} ohm-cm at 900°F) is low enough to cause appreciable eddy current loss. The advantage of T-111's high strength is also partly overcome by its high density of 0.60 lb/in³.

Blank Page

APPENDIX B

NOTES ON PROCEDURE FOR CALCULATION OF ROTOR STRESSES

The stresses within the rotor core were determined by the procedures outlined below:

A. Pole Root Stress

1. The mass and the center of the mass within a pole relative to the axis of rotation were determined from the dimensions of the rotor.
2. The centrifugal force acting through the center of the mass within a pole is then determined.
3. Equilibrium between the centrifugal force acting upon the pole and uniform radial stress on the arc embraced by the rotor core and pole root was assumed. Thus,

$$\text{Pole root stress} = \frac{\text{Centrifugal force per unit of axial length}}{2 \times \text{rotor core radius} \times \sin\left(\frac{\text{arc}}{2}\right)}$$

B. Fourier Series for Radial Loads (Pole Root Stress) On the Rotor Core

1. The load distribution on the rotor core was expressed as periodic function of θ as shown below:

where

M = number of pole on each end of rotor
 $2\pi/M$ = angle between pole
 α = half angle of arc embraced by pole
P = pole root stress

2. For this case the Fourier series becomes

$$P(\theta) = P_{avg} + \frac{2PM}{\pi} \sum_{\substack{x=kM \\ k=1,2,3}}^{\infty} \frac{1}{x} \sin x\alpha \cos x\theta$$

where

$$P_{avg} = \text{average uniform stress on the root core} = \frac{MP\alpha}{\pi}$$

C. Stress Due to External Load Which is Uniform

The stresses within the rotor core due to P_{avg} were calculated using Lamé's formulae for hollow non-rotating cylinder under an external load. Since the rotor core was solid, for this case,

$$\sigma_{\theta} = \sigma_R = P_{avg}$$

D. Stress Due to External Loads Which Are a Periodic Function of θ

1. The stresses within the rotor core due to the external load which are a periodic function of θ were calculated using the following Airy's stress function

$$\phi = \sum_{n=2,3,4}^{\infty} (a_n R^n + b_n R^{n+2} + c_n R^{-n+2}) \cos n\theta$$

This stress function satisfies the biharmonic equation of compatibility and the corresponding stress components are:

$$\sigma_R = \frac{1}{R} \frac{\partial \phi}{\partial R} + \frac{1}{R^2} \frac{\partial^2 \phi}{\partial \theta^2}$$

$$\sigma_{\theta} = \frac{\partial^2 \phi}{\partial R^2}$$

$$\sigma_s = - \frac{\partial}{\partial R} \left(\frac{1}{R} \frac{\partial \phi}{\partial \theta} \right)$$

Thus,

$$\sigma_R = \sum_{n=2}^{\infty} \cos n\theta [-n(n-1)a_n R^{n-2} + (n-2)(n+1)b_n R^{n-1} + n(n+1)c_n R^{-(n+2)} - (n+2)(n-1)d_n R^{-n}]$$

$$\sigma_\theta = \sum_{n=2}^{\infty} \cos n\theta [n(n-1)a_n R^{n-2} + (n+2)(n+1)b_n R^{n+1} + n(n+1)c_n R^{-(n+2)} + (n-2)(n-1)d_n R^{-n}]$$

$$\sigma_S = \sum_{n=2}^{\infty} \sin n\theta [n(n-1)a_n R^{n-2} + n(n+1)b_n R^{n-1} + n(n+1)c_n R^{-(n+2)} - n(n-1)d_n R^{-n}]$$

2. For our case we are only interested in the solutions for $n = X$ as indicated by the Fourier series defined in Part B-2. The number of terms in the above equation were also reduced by letting:

$$n(n-1)a_n = X(X-1)a_X = A_X$$

$$(n+1)b_n = (X+1)b_X = B_X$$

$$n(n+1)c_n = X(X+1)c_X = C_X$$

$$(n-1)d_n = (X-1)d_X = D_X$$

These substitutions gave the following system of equations:

$$\sigma_R = \sum_{x=1M, 2M--}^{\infty} \cos X\theta [A_X R^{X-2} + (X-2)B_X R^X + C_X R^{-(X+2)} - (X+2)D_X R^{-X}]$$

$$\sigma_\theta = \sum_{x=1M, 2M--}^{\infty} \cos X\theta [A_X R^{X-2} + (X-2)B_X R^X + C_X R^{-(X+2)} + (X+2)D_X R^{-X}]$$

$$\sigma_S = \sum_{x=1M, 2M--}^{\infty} \sin X\theta [A_X R^{X-2} + XB_X R^X - C_X R^{-(X+2)} - XD_X R^{-X}]$$

3. The constants A_x , B_x , C_x , and D_x , which are a function of X , were evaluated using the following boundary conditions:

- (a) For solid rotor core without a central hole $C_x = D_x = 0$, since the stress must be finite at $R = 0$. At the outer boundary of the core, where $R = b$, $\sigma_s = 0$ and

$$\sigma_R = \frac{2PM}{\pi} \sum_{x=1M, 2M, \dots}^{\infty} \frac{1}{X} \sin X\alpha \cos X\theta$$

- (b) For a rotor core with a central hole where $R = a$, $\sigma_s = 0$ and $\sigma_r = 0$. At the outer boundary of the core where $R=b$, $\sigma_s = 0$ and

$$\sigma_R = \frac{2PM}{\pi} \sum_{x=1M, 2M, \dots}^{\infty} \frac{1}{X} \sin X\alpha \cos X\theta$$

4. The system of equations which satisfy these boundary conditions are given on page 229. These equations were used to determine the stress components produced by the poles as a function of R and θ . These stress components were added to the stress components produced by the average external load on the core to obtain the total stress due to the centrifugal load of the poles.

E. Centrifugal Stresses Within Rotor Core

The rotor core was treated as a flat rotating disk and the tangential and radial stress were calculated using the standard formulae for this problem.

F. Combined Stresses

The stresses due to the centrifugal forces within the rotor core were added to the stresses produced by the centrifugal loading of the poles to obtain the total combined stress distribution in a two-dimensional plane through the pole and perpendicular to the axis of rotation. After the combined radial and combined tangential stresses and shearing stress at a point were determined, a maximum and minimum principal stress was determined using the familiar Mohr formulae for two-dimensional stress.

$$\sigma_R = \frac{2PM}{\pi} \sum_{x=1M, 2M, 3M}^{\infty} \{ (x-1)Q^2S^x + JS^{x-2} - (x-2)S^x - U^xQ^{x+2} + (x+1)U^xQ^xS^{-2}J - (x+2)U^{x-2}Q^xJ \} \frac{\cos x\theta \sin x\alpha}{TX}$$

$$\sigma_\theta = \frac{2PM}{\pi} \sum_{x=1M, 2M, 3M}^{\infty} - \{ (x-1)Q^2S^x + JS^{x-2} - (x+2)S^x - U^xQ^{x+2} + (x+1)U^xQ^xS^{-2}J - (x-2)U^{x-2}Q^xJ \} \frac{\cos x\theta \sin x\alpha}{TX}$$

$$\sigma_s = \frac{2PM}{\pi} \sum_{x=1M, 2M, 3M}^{\infty} - \{ (x-1)Q^2S^x + JS^{x-2} - XS^x + U^xQ^{x+2} - (x+1)U^xQ^xS^{-2}J + XU^{x-2}Q^xJ \} \frac{\sin x\theta \sin x\alpha}{TX}$$

$$Q = \frac{a}{R}, \quad S = \frac{R}{b}, \quad U = \frac{a}{b}$$

$$J = \frac{x - (x-1)U^2 - U^{2x+2}}{xU^{2x-2} - (x-1)U^{2x+1}}$$

$$T = (x-1)U^2 + J - x + 2 + U^{2x+2} + (x+1)JU^{2x} - (x+2)JU^{2x-2}$$

G. Comparison of Biaxial Tensile Stresses which Exist in the Rotor with Data Obtained from Uniaxial Tensile Test

The maximum and minimum principal stresses were used to calculate an equivalent stress intensity for comparison with uniaxial tensile data for rotor materials. An equation based on the Von Mises criterion for failure was used to calculate the equivalent stress intensity. For this case,

$$\sigma_M(\text{equivalent stress}) = \sqrt{\sigma_1^2 + \sigma_2^2 - \sigma_1 \sigma_2}$$

where σ_1 and σ_2 are the maximum and minimum principal stress.