MASTER

SAN-679-2

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

DESIGN STUDY

Quarterly Technical Progress Report No. 2
December 1, 1966 — February 28, 1967

Edited by
A. E. King

August 1967
[DTIE Issuance Date]

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.

Quarterly Technical Progress Report No. 2

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

February 28, 1967

COPIES
H.C. \$93.00, MA .65

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.

Edited by:

A. E. King
A. E. King, Project Engineer
Liquid Metal Development Section

Approved by:

F. C. Stump
F. C. Stump, Program Manager
Liquid Metal Development Section

N. W. Bucci
N. W. Bucci, Engineering Manager
Systems R & D 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

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

FOREWORD

This Quarterly Technical Progress Report describes work under contract to the United States Atomic Energy Commission from December 1, 1966 to February 28, 1967, 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, D. R. Basel, R. C. Fear, R. D. Jessee, C. C. Kouba, J. L. McCabria, E. F. Swiderski, and D. G. Berkowitz.

ABSTRACT

Progress on parametric studies to determine minimum system weight as a function of electrical component rating, voltage, frequency and speed are presented on a-c generators, a-c motors, power conditioning and power distribution equipment for 0.25 to 10.0 megawatt space nuclear electric power plants utilizing the Rankine conversion cycle. The power conditioning and distribution equipment includes transformers, rectifiers, frequency changers, contactors and transmission lines. Final parametric data is presented on frequency changers in the 5 to 500 kva range as well as on contactors in the 0.05 to 10.0 mva range. Electrical power uses and missions analyses are presented to illustrate electrical load breakdown on probable missions requiring 0.25 to 10 megawatts of power. Progress on experiments to verify analytical techniques for inductor generator designs typical of high-power, high-speed, high-frequency, high-temperature applications is presented. Analysis of tests to verify magnetic forces between an eccentric rotor and stator of an inductor alternator is included.

CONTENTS

1.	SUMMARY	1
1.1	Events of the Period	2
1.2	Plans for Coming Reporting Period	4
2.	PARAMETRIC SCREENING STUDIES	5
2.1	Generator Parametric Study	5
2.1.1	Technical Discussion	5
2.1.2	Plans for Coming Quarter	56
2.2	AC Motor Parametric Study	60
2.2.1	New and Revised Definitions of Motor Design Parameters or Data	60
2.2.2	Status of Parametric Study	72
2.2.3	Plans for the Next Quarter	83
2.3	Frequency Changer Study	84
2.3.1	Design Concepts	84
2.3.2	Cycloconverter Ratings	86
2.3.3	Design Calculations	88
2.3.4	Mechanical Design	110
2.3.5	Weight and Efficiency Calculation Results	116
2.4	Power Conditioning Study	125
2.4.1	Rectifier Designs	125
2.4.2	Transformer Designs	130
2.4.3	Plans for Coming Quarter	138
2.5	Transmission and Distribution Study	140
2.5.1	Transmission Lines	140
2.5.2	Contactors Design Evaluation	148
2.5.3	Plans for Coming Quarter	170
3.	SYSTEM DEFINITIONS	171
3.1	Electrical Loads Definition	171
3.1.1	Scope of Evaluation	171
3.1.2	Mission Loads Determination	172
3.1.3	Mission Loads Evaluation	179
3.1.4	Individual Loads Evaluation	182
3.1.5	Contributing System Factors.....	186
3.1.6	Plans for Coming Quarter	191
3.2	Nuclear Mechanical Systems	192
3.2.1	System Specific Weights	192
3.2.2	Effective System Weights	194
3.2.3	Rotor Inertia and Transient Response	198
3.2.4	Plans for Coming Quarter	200
4.	EXPERIMENTAL VERIFICATION	203
4.1	Magnetic Force Unbalance and Flux Distribution..	203
4.1.1	Magnetic Force Unbalance	203
4.1.2	Magnetic Flux Distribution Tests	222
4.2	Magnetic No-Load Loss Tests	224

CONTENTS (continued)

4.2.1	Past Quarter Accomplishments	224
4.2.2	Plans for Next Quarter	224
4.3	Rotor Cooling Concepts	226
4.3.1	Past Quarter Accomplishments	226
4.3.2	Plans for Coming Quarter	226
REFERENCES		230

FIGURES

2.1-1	400 KVA Generator Frequency Study First Cycle Designs	11
2.1-2	1200 KVA Generator Frequency Study First Cycle Designs	12
2.1-3	6 MVA Generator Frequency Study First Cycle Designs	13
2.1-4	12 MVA Generator Frequency Study First Cycle Designs	14
2.1-5	Creep Strength Versus Temperature for 0.2% Creep Strain in 10,000 Hours	15
2.1-6	400 KVA Generator Speed Study First Cycle Designs	23
2.1-7	1200 KVA Generator Speed Study First Cycle Designs	24
2.1-8	6 MVA Generator Speed Study First Cycle Designs..	25
2.1-9	12 MVA Generator Speed Study First Cycle Designs.	26
2.1-10	400 KVA Generator Voltage Study First Cycle Design	34
2.1-11	1200 KVA Generator Voltage Study First Cycle Design	35
2.1-12	6 MVA Generator Voltage Study First Cycle Design.	36
2.1-13	12 MVA Generator Voltage Study First Cycle Design	37
2.1-14	Generator Rotor Weight at Various Design Speeds .	39
2.1-15	Chart for Determining Eccentricity Ratio	43
2.1-16	Charts for Determining Aspect Factors	44
2.1-17	Minimum Bearing Bore (BB) as a Function of Design Speed N	45
2.1-18	Minimum R/c Ratios as a Function of Design Speed N	46
2.1-19	Chart for Determining Coefficient of Friction ...	48
2.1-20	Stub Shaft Configuration	49
2.1-21	First Resonant Speeds for Selected Rotor/Bearing Stub Shaft Configurations	51
2.1-22	Basic Shaft Seal Configuration	55
2.1-23	Typical Holweck Pump Performance in 800°F Potassium	57
2.1-24	Typical Holweck Pump Performance in 1100°F Potassium	58
2.1-25	Density of Potassium Vapor Surrounding Rotor	59
2.2-1	Larson-Miller Parameter = $T (15 + \log t) \times 10^{-3}$..	66
2.2-2	5 HP Motor Frequency Study First Cycle Designs...	78
2.2-3	5 HP Motor Frequency Study First Cycle Designs...	78
2.2-4	30 HP Motor Frequency Study First Cycle Designs .	79
2.2-5	30 HP Motor Frequency Study First Cycle Designs .	79
2.2-6	60 HP Motor Frequency Study First Cycle Designs .	80
2.2-7	60 HP Motor Frequency Study First Cycle Designs .	80
2.2-8	100 HP Motor Frequency Study First Cycle Designs.	81
2.2-9	100 HP Motor Frequency Study First Cycle Designs.	81
2.2-10	250 HP Motor Frequency Study First Cycle Designs.	82

FIGURES (continued)

2.2-11	250 HP Motor Frequency Study First Cycle Designs .	82
2.3-1	Cycloconverter - Basic Power Circuit	85
2.3-2	Thyristor Arrangement	87
2.3-3	Iron-Core Inductor.....	103
2.3-4	Weight Versus Frequency	106
2.3-5	Weight Versus Frequency as a Function of KVA	107
2.3-6	Transformer Weight	108
2.3-7	Frequency Changer System Cooling	111
2.3-8	Frequency Changer Parametric Data	113
2.3-9	Frequency Changer Parametric Data	114
2.3-10	Frequency Changer Parametric Data	115
2.3-11	Frequency Changer Parametric Summary	117
2.3-12	Frequency Changer Parametric Summary	118
2.3-13	Frequency Changer Parametric Summary	119
2.3-14	Frequency Changer Parametric Summary	120
2.3-15	Frequency Changer Parametric Summary	121
2.3-16	Frequency Changer Parametric Summary	122
2.3-17	Frequency Changer Parametric Summary	123
2.3-18	Frequency Changer Study Summary	124
2.4-1	Rectifier System Cooling	131
2.4-2	Rectifier Parametric Data	132
2.4-3	Rectifier Parametric Data	133
2.4-4	Rectifier Parametric Data	134
2.4-5	Rectifier Parametric Data	135
2.4-6	Rectifier Parametric Data	136
2.4-7	Rectifier Parametric Data	137
2.5-1	Weight Versus Losses as a Function of Frequency for 50 Amps Current	144
2.5-2	Weight Versus Losses as a Function of Frequency for 400 Amps Current	145
2.5-3	Voltage Drop per Unit Length Versus Frequency for a Specified Conductor Size	146
2.5-4	Weight Versus Losses as a Function of Current for 2000 cps Frequency	147
2.5-5	Weight Versus Voltage for 3-phase, AC PM Latch Vacuum Contactors, 100°C Ambient	156
2.5-6	Weight Versus Voltage for 3-phase, AC PM Latch Vacuum Contactors, 100°C and 500°C Ambient	157
2.5-7	Weight Versus Voltage for 3-phase, AC PM Latch Vacuum Contactors, 10,000 KVA, 100°C and 500°C Ambient	158
2.5-8	Weight Versus Voltage for 3-phase, AC Vacuum Contactors, 100°C Ambient, 1000 KVA	159
2.5-9	Volume Versus Voltage for 3-phase AC PM Latch Vacuum Contactors, 100°C Ambient	160
2.5-10	Volume Versus Voltage for 3-phase AC PM Latch Vacuum Contactors, 100°C and 500°C Ambient	161

FIGURES (continued)

2.5-11	Volume Versus Voltage for 3-Phase AC PM Latch Vacuum Contactors, 10 MVA, 100°C and 500°C Ambient	162
2.5-12	Volume Versus Voltage for 3-Phase Vacuum Contactor 10,000 KVA, 100°C Ambient	163
2.5-13	Losses as a Function of Voltage for 3-Phase AC PM Latch Vacuum Contactors, 100°C Ambient	164
2.5-14	Losses as a Function of Voltage for 3-Phase AC PM Latch Vacuum Contactor, 100°C and 500°C Ambient .	165
2.5-15	Losses as a Function of Voltage for 3-Phase AC Permanent Magnet Latch Vacuum Contactor, 10,000 KVA, 100°C and 500°C Ambient	166
2.5-16	Losses as a Function of Voltage for 3-Phase PM Latch Vacuum Contactors, 100 MVA, 100°C Ambient..	167
3.2-1	Basic Rankine Cycle Configuration	193
3.2-2	Rotor Kinetic Energy Versus Generation Electrical Rating	201
3.2-3	Time Required to Accelerate Rotor to 5 Percent Overspeed Versus Generator Electrical Rating	202
4.1-1	Rotor with Search Coils and Bearings	204
4.1-2	Stator Test Fixture	205
4.1-3	End View of 16 Pole Rotor	208
4.1-4	Rotor Eccentric to Stator	209
4.1-5	Two Pole Rotor	209
4.1-6	Rotor Poles and Flux Distribution	210
4.1-7	Radial Gap Per Unit Specific Permeance	211
4.1-8	Flux Distribution	213
4.1-9	Specific Permeance, 30 Mil Eccentricity	214
4.1-10	Force Distribution, 30 Mil Eccentricity	215
4.1-11	Specific Permeance, 5 Mil Eccentricity	216
4.1-12	Force Distribution, 5 Mil Eccentricity	217
4.1-13	Eccentric Rotor and Force Diagram	218
4.1-14	Force Diagram	218
4.1-15	Radial Magnetic Force on Rotor	219
4.1-16	Typical Generator Saturation Curve	220
4.1-17	Maximum Radial Magnetic Force on Rotor	221
4.2-1	No-Load Dynamic Test Rig	225
4.3-1	Fluid Dynamic Test Rig	227
4.3-2	Antidrive-end View	228
4.3-3	Drive-end View	229

BLANK

TABLES

2.1-1	Summary of 400 KVA Generator Designs, Frequency Study, First Cycle, 24,000 RPM, 1000 Volts, L-N	7
2.1-2	Summary of 1200 KVA Generator Designs, Frequency Study, First Cycle, 15,000 RPM, 1000 Volts, L-N	8
2.1-3	Summary of 6 MVA Generator Designs, Frequency Study, First Cycle, 10,000 RPM, 1000 Volts, L-N ..	9
2.1-4	Summary of 12 MVA Generator Designs, Frequency Study, First Cycle, 8000 RPM, 1000 Volts, L-N	10
2.1-5	Changes in Materials and Generator Parameters Following First Cycle Frequency Study	17
2.1-6	Summary of 400 KVA Generator Designs, Speed Study, First Cycle, 2000 Hz, 1000 Volts, L-N	19
2.1-7	Summary of 1200 KVA Generator Designs, Speed Study, First Cycle, 2000 cps, 1000 Volts, L-N	20
2.1-8	Summary of 6 MVA Generator Designs, Speed Study, First Cycle, 1200 cps, 1000 Volts, L-N	21
2.1-9	Summary of 12 MVA Generator Designs, Speed Study, First Cycle, 1200 cps, 1000 Volts, L-N	22
2.1-10	Summary of 400 KVA Generator Designs, Voltage Study, First Cycle, 2000 cps	29
2.1-11	Summary of 1200 KVA Generator Designs, Voltage Study, First Cycle, 2000 cps	30
2.1-12	Summary of 6 MVA Generator Designs, Voltage Study, First Cycle, 1200 cps	31
2.1-13	Summary of 12 MVA Generator Designs, Voltage Study, First Cycle, 1200 cps	32
2.1-14	Summary of Results from First Cycle Generator Design Studies	33
2.2-1	Synchronized Speeds for Various Combinations of Poles and Frequencies	62
2.2-2	Summary of 5 HP Motor Designs, Frequency Study, First Cycle, RPM = 12,000; Frequency - Variable, L-N Voltage = 120	73
2.2-3	Summary of 30 HP Motor Designs, Frequency Study, First Cycle, RPM = 12,000; Frequency - Variable, L-N Voltage = 120	74
2.2-4	Summary of 60 HP Motor Designs, Frequency Study, First Cycle, RPM = 12,000; Frequency - Variable, L-N Voltage = 120	75
2.2-5	Summary of 100 HP Motor Designs, Frequency Study, First Cycle, RPM = 12,000; Frequency - Variable, L-N Voltage = 120	76
2.2-6	Summary of 250 HP Motor Designs, Frequency Study, First Cycle, RPM = 12,000; Frequency - Variable, L-N Voltage = 120	77
2.3-1	Cycloconverter Design Summary, 200 volt, 2000/400 cps, Power Factor 0.5	91

TABLES (continued)

2.3-2	Cycloconverter Design Summary, 200 volt, 2000/400 cps, Power Factor 0.7	92
2.3-3	Cycloconverter Design Summary, 50 KVA, 200/400 cps, Power Factor 0.7	93
2.3-4	Cycloconverter Design Summary, 50 KVA, 200 volt, 800 cps Input, Power Factor 0.7	94
2.3-5	Cycloconverter Design Summary, 50 KVA, 200 volt, 1200 cps Input, Power Factor 0.7	95
2.3-6	Cycloconverter Design Summary, 200 volt, 2000 cps Input, Power Factor 0.7	96
2.3-7	Cycloconverter Design Summary, 50 KVA, 200 volt, 3200 cps Input, Power Factor 0.7	97
2.4-1	1 Kilovolt DC Silicon Diode Rectifier Assembly Systems	126
2.4-2	5 Kilovolt DC Silicon Diode Rectifier Assembly Systems	127
2.4-3	10 Kilovolt DC Silicon Diode Rectifier Assembly Systems	128
2.4-4	30 Kilovolt DC Silicon Diode Rectifier Assembly Systems	129
2.4-5	Transformer Materials and Temperatures	138
2.4-6	Transformer Cooling System Materials and Tempera- tures	139
2.5-1	Parameter Ranges and Data Points for Transmission and Distribution Evaluation	141
2.5-2	Radius Ratio - Relating Skin Effect to Radius Ratio.	142
3.2-1	Powerplant Component Weights	195

1. SUMMARY

The Aerospace Electrical Division of the Westinghouse Electric Corporation has been under contract to the United States Atomic Energy Commission since September 1, 1966 to define conceptual designs of complete electrical power systems and the electrical components therein which are suitable for Rankine nuclear space power systems in the 0.25 to 10.0 megawatt range. Three representative missions- interplanetary, planetary, and orbital - have been specified in order that the conceptual definitions consider dependent design parameters typical of actual flight conditions. Work is being carried on in design studies and in associated analytical verification experiments.

The design studies are being carried out in two successive stages: (1) analytical screening studies to establish parametric data, optimum designs and design limitations for four power levels, and (2) detailed studies of selected configurations to determine the best conceptual design at each power level and the development problems associated with them. The electrical system components being studied are a-c generators, a-c motors, frequency changers, power conditioning equipment, power transmission and control equipment, and electrical system control and protection equipment. The results of the studies will 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.

The analytical verification experiments are being carried out to evaluate the adequacy of certain analytical techniques for a-c generator design. The experiments are 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 table in order for the reader to better understand the extent of the work being carried out and the relationships of the various work described in the following sections. There are three major tasks which contain a total of twelve separate but closely 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 Events of the Period

The Parametric Screening Studies continued through to various stages of completion as discussed below. Work was initiated on the Conceptual Design Analyses; most of the work was limited to laying the ground rules for the Electrical System Definition portion of the task. The Magnetic Unbalance Force and Flux Distribution Tests were completed with several significant findings being reported. With the completion of these static tests, preparation of the rotor and stator for the No-Load Dynamic Tests was initiated. The Rotor Coolant Flow Experiments were started in mid-February. The status of the individual sub-tasks are summarized below.

The Power Uses and Missions Definition task was completed and is summarized in this report. One of the general conclusions drawn from the review of several recent mission studies was that the majority of the power is utilized as d-c throughout the 0.25 to 10.0 megawatt range. However, the relative (percentage) division and voltage levels are very much dependent upon mission objectives.

Work was continued on the Nuclear Mechanical System Definitions to determine the various prime-mover component weights and specific weights. Generator rotor overspeed transients were also analyzed.

The Generator Parametric Studies were carried through to near completion. The first cycle screening runs were completed and the conditions were established for the subsequent second cycle (refining) which is over one-half complete. In general, the results of the first cycle showed the optimum frequencies occurred in the range of 1200 to 2000 Hz and the optimum voltages were in the range of 500 to

1000 volts. The optimum speeds were as high as 30000 rpm for the 0.4 mva, 500°F designs and as low as 1000 rpm for the 12 mva, 1100°F designs.

The Motor Parametric Frequency Studies were nearly completed and the speed screening studies were initiated. In general, frequencies in the 400 to 800 cps range resulted in the lightest motor weights and the lowest winding temperatures.

The Frequency Changer Parametric Study was completed except for the evaluation of high-temperature gas-tube rectifiers in place of the solid state devices.* The parametric analyses showed that the frequency changer is generally over 97 percent efficient and that the weight increases rather mildly below 100 kva but rapidly above that rating; weight also increases rapidly at higher voltages, e.g., over 200 volts.

On the Power Conditioning Parametric Study, the analyses of rectifier designs were brought within 20 percent of completion for the solid state designs. Preliminary analysis of the gas-tube data indicates that their usefulness may be limited to kilo-volt ranges; further clarification of this conclusion is being sought. Data and parameters for the transformer portion of this study have been completed for input to the computer program.

Computer runs were completed on the Transmission Line Parametric Study near the end of the reporting period; analysis of the data is in progress. The contactor portion of the Transmission and Distribution Study is summarized in this report. It was generally concluded that the availability and technology for large-rating contactors suitable to space power is lacking, especially for d-c contactors.

The tests for the Experimental Evaluation of Magnetic Unbalance Force and Flux Distribution were finished and the analysis of the data was nearly completed. The magnetic unbalance forces were found to agree quite well with the predicted forces. Analysis of the flux distributions is continuing. Preliminary results indicate that circumferential flux distributions in the armature stack DBS (depth-behind-slot) are less than expected and could result in lower losses and a net increase in generator efficiency of about three percentage points. Further verification of this will be accomplished by the No-Load Loss Tests.

Since the above static tests have been completed, work has been progressing on the alterations necessary to convert the generator components for the No-Load Loss Tests. Work is progressing satisfactorily.

*The applicability of these rectifiers to frequency changer designs is being carefully examined before committing them to parametric design analysis.

The test components and set-up for the Rotor Cooling Flow Experiments were completed and the tests begun in mid-February. No specific conclusions could be drawn at the time of this writing.

1.2 Plans for Coming Reporting Period

The Parametric Screening Studies will be completed in the next reporting period and the data utilized to define representative Electrical Systems. Following this, the most representative designs will be selected for the conceptual design effort. Primary emphasis will be placed on the generator conceptual designs.

The remaining Analytical Verification Experiments will be completed.

Final and topical reports to document the results of the various tasks will be completed.

2. PARAMETRIC SCREENING STUDIES

2.1 Generator Parametric Study

ABSTRACT

During the second quarter, 194 generator designs were analyzed and compared in terms of generator electrical weight and in terms of powerplant and cooling system weight penalties to determine the best ranges of frequencies, speeds, and voltages for ratings of 400 kva, 1200 kva, 6 mva, and 12 mva and coolant temperatures of 500, 800, and 1100°F.

The results obtained on the first cycle designs will be used in the second cycle study to more specifically determine the best combinations of frequency, speed and voltage for the above ratings and coolant temperatures.

2.1.1 Technical Discussion

A. First Cycle Generator Design Studies

1. Frequency Study - A frequency study was first conducted by calculating generator designs combining the following parameters:

<u>Generator Rating (mva)</u>	<u>Coolant Temp. (°F)</u>	<u>RPM</u>	<u>Frequency Points (Hz)</u>
0.4	800	24000	800, 1200, 2000, 2800, 3200
1.2	800	15000	500, 1000, 2000, 2500, 3000
6.0	1100	10000	500, 1000, 2000, 2500, 3000
12.0	500	8000	400, 1200, 2000, 2800, 3200

1000 Volts line-to-neutral was used for all frequency study designs above.

The choices of voltage and speeds were based on values which appeared attractive from earlier studies.(1)*

*Superscripts in parentheses refer to references at end of report.

Both H-11 and NIVCO rotor steels were used in designs using 800°F coolant to compare the good magnetic-lower strength properties of H-11 to the poorer magnetic-higher strength properties of NIVCO.

The designs at each temperature and rating are compared on the basis of generator electrical weight and on the basis of generator electrical weight plus estimated power plant and cooling system weight penalty in Tables 2.1-1 through 2.1-4. (Weight penalty is determined from the generator efficiency and the specific weights of the power plant and cooling systems that supply and dissipate the losses; see Section 3.2 . Presently, the specific weights are preliminary estimates which will be verified shortly so that curves of weight penalty versus generator efficiency may be shown in a future report). Figures 2.1-1 through 2.1-4 show curves of generator weight (with and without the weight penalties) versus frequency.

Tables 2.1-1 and 2.1-2 and Figures 2.1-1 and 2.1-2 for 400 kva and 1200 kva ratings show that generator electrical weight tends to be lowest in the 2000 to 3000 Hz range while generator weight plus system weight penalty is lowest in the 1000 to 2000 Hz range. Based on this data, a frequency of 2000 Hz was chosen for use in 400 and 1200 kva designs in the subsequent speed and voltage studies.

Tables 2.1-3 and 2.1-4 and Figures 2.1-3 and 2.1-4 for 6 and 12 mva ratings show a generator weight advantage in the 1000 to 2000 Hz range and a system weight advantage in the 1000 to 1400 Hz range. Therefore, a frequency of 1200 Hz was chosen for use in 6 and 12 mva designs in the subsequent speed and voltage studies.

According to results shown in Tables 2.1-1 through 2.1-4, the majority of 800°F designs using NIVCO rotor material exceeded the allowable stress limits while the 800°F designs using H-11 rotor material were satisfactory from a stress standpoint and also lighter in weight. The stress capabilities of NIVCO and H-11 are nearly equal at equal temperatures in the 800°F range (Figure 2.1-5); however, the poorer magnetic properties of NIVCO require a larger rotor for a given rating resulting in greater stresses and a greater temperature drop in the rotor between the stressed portions and the coolant. For a given rating and coolant temperature, portions of a NIVCO rotor will operate at a higher temperature and stress than would be encountered using a smaller H-11 rotor, thus the stress capabilities of the NIVCO rotor will be lower than those of an equivalent H-11 rotor.

All 1100°F designs in the first frequency study exceeded the stress limits of the NIVCO rotor material at the chosen speeds. This prompted the choice of lower speed ranges for 1100°F designs in the subsequent speed study.

Although designs which will later be chosen for detailed study may have their cooling circuits improved and temperatures lowered from those estimated by preliminary calculations, it was desirable to choose parameters for the preliminary designs which would

Table 2.1-1 Summary of 400 KVA Generator Designs, Frequency Study, First Cycle,
24000 RPM, 1000 Volts, L-N

Design No.	Average Coolant Temp. (°F)	Frequency (Hz)	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. Xd	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)
10501	500	800	761	89.2	2093	1611	0.94	H-11	22.68	15.15	105200	52122
02	↓	1200	530	89.4	1670	1450	0.96	↓	21.18	13.27	116400	46309
03	↓	2000	432	88.7	1202	1432	0.89	↓	20.42	12.12	122200	45032
04	↓	2800	419	85.3	1322	1809	0.99	↓	20.57	11.90	124000	46494
05	↓	3200	439	82.4	1321	2218	0.94	↓	20.97	11.91	124400	48665
10801	800	800	776	88.6	2882	1606	0.96	H-11	22.84	15.33	69100	53708
02	↓	1200	565	89.2	2042	1345	0.89	↓	21.64	13.58	76500	48203
03	↓	2000	450	88.0	1602	1330	1.14	↓	20.90	12.88	80200	44115
04	↓	2800	472	84.5	1567	1672	0.90	↓	21.48	12.50	81500	47794
05	↓	3200	462	83.3	1642	1762	0.98	↓	21.51	12.16	81600	49397
10806	800	800	866	85.0	4402	2016	1.05	Nivco	25.64	14.51	69100	82718
07	↓	1200	597	86.9	2782	1577	1.02	↓	22.94	12.82	76500	71064
08	↓	2000	741	87.9	1436	1631	0.88	↓	24.71	12.38	80200	91568
09	↓	2800	621	82.9	1267	1961	1.17	↓	24.01	12.19	81500	81175
10	↓	3200	670	82.3	1417	2050	0.92	↓	24.55	11.76	81600	88790
11101	1100	800	840	82.3	3952	2190	1.04	Nivco	25.26	14.06	14320	83040
02	↓	1200	569	85.6	2597	1619	1.03	↓	22.29	12.28	16380	71426
03	↓	2000	490	86.0	1922	1494	0.93	↓	21.97	10.76	16620	72235
04	↓	2800	574	81.0	1817	2024	1.03	↓	23.09	10.73	16930	85113
05	↓	3200	569	79.2	1727	2219	1.13	↓	23.22	11.20	16990	81482

Table 2.1-2 Summary of 1200 KVA Generator Designs, Frequency Study, First Cycle
15000 RPM, 1000 Volts L-N

Design No.	Average Coolant Temp. (°F)	Frequency (Hz)	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. Xd	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)
20501	500	500	3182	82.9	7252	7732	1.24	H-11	33.69	24.71	105200	50192
02	↓	1000	1494	91.8	2252	3394	1.18	↓	29.05	17.04	120300	37506
03		2000	1326	90.6	1302	3479	1.13		29.06	15.72	124400	37193
04		2500	1361	89.9	1162	3660	0.99		29.38	15.56	125000	38991
05		3000	1390	87.6	1216	4839	1.09	↓	29.75	15.42	125000	40430
20801	800	500	3208	84.9	6282	6108	1.23	H-11	34.09	24.35	69100	51600
02	↓	1000	1538	90.3	2952	3288	1.22	↓	29.22	17.51	79000	38789
03		2000	1327	90.8	1677	2967	1.13		29.13	15.71	81600	37935
04		2500	1275	89.9	1620	3075	0.99		28.93	14.83	82000	39270
05		3000	1411	88.0	1520	3611	1.04	↓	30.11	15.00	82000	42040
20806	800	500	3281	62.8	11840	>8000	1.28	Nivco	39.11	20.17	69100	80766
07	↓	1000	2111	90.3	3132	3861	0.88	↓	33.12	17.08	79000	66682
08		2000	2418	90.1	1580	4198	0.97		35.70	16.48	81600	79880
09		2500	1198	41.5	-	>6000	1.24		29.73	12.48	82000	54150
10		3000	2172	86.2	1647	4772	1.06	↓	34.98	15.16	82000	80054
21101	1100	500	2996	49.8	11040	>8000	1.30	Nivco	38.80	18.76	14320	78599
02	↓	1000	2008	88.9	3072	3968	0.96	↓	32.51	16.88	16380	65243
03		2000	1373	89.5	1932	3213	0.97		30.21	14.00	16990	54953
04		2500	1608	86.9	2022	3988	1.49		31.71	13.98	17000	64437
05		3000	2107	84.1	1822	5097	1.03	↓	34.43	14.85	17000	79736

Table 2.1-3 Summary of 6 MVA Generator Designs, Frequency Study, First Cycle
10000 RPM, 1000 Volts L-N

Design No.	Average Coolant Temp. (°F)	Frequency (Hz)	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. Xd	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)
30501	500	500	8722	83.7	7782	27222	1.20	H-11	54.29	23.16	116400	58803
02	↓	1000	5581	85.9	6012	20580	1.46	↓	45.80	22.16	123300	50783
03	↓	2000	5878	86.4	3822	20277	1.13	↓	47.46	21.80	125000	55295
04	↓	2500	7378	85.9	2362	22377	0.83	↓	48.67	26.48	125000	61279
05	↓	3000	5418	78.3	-	33918	1.74	↓	45.40	30.33	125000	65890
30801	800	500	9106	84.2	9602	22605	1.17	H-11	56.29	22.43	76500	62415
02	↓	1000	5619	87.8	5842	15419	1.45	↓	46.17	21.86	81000	52124
03	↓	2000	7890	89.5	2172	16189	0.78	↓	48.72	28.56	82000	62236
04	↓	2500	7733	88.7	2232	16732	0.85	↓	49.05	27.47	82000	63809
05	↓	3000	5380	83.6	-	19380	1.71	↓	45.57	28.45	82000	67337
30806	800	500	19460	79.2	5432	39460	1.09	Nivco	75.40	23.33	76500	150850
07	↓	1000	7935	86.5	5842	18935	1.17	↓	51.80	24.02	81000	86126
08	↓	2000	8656	87.9	2597	18456	0.64	↓	54.51	22.60	82000	98241
09	↓	2500	11746	84.5	2502	24746	0.84	↓	60.64	23.05	82000	125166
10	↓	3000	12177	83.2	2492	26677	1.05	↓	61.18	23.44	82000	129484
31101	1100	500	14451	76.9	13570	35451	1.53	Nivco	68.93	21.79	15850	125608
02	↓	1000	7808	88.1	4782	17208	1.14	↓	51.88	23.28	16900	86812
03	↓	2000	10186	85.1	3122	22186	1.19	↓	60.85	19.02	17000	121320
04	↓	2500	11759	84.0	2280	22759	0.82	↓	61.07	22.43	17000	127501
05	↓	3000	12354	82.1	2202	27354	1.16	↓	60.67	24.15	17000	129028

Table 2.1-4 Summary of 12 MVA Generator Designs, Frequency Study, First Cycle,
8000 RPM, 1000 Volts L-N

Design No.	Average Coolant Temp. (°F)	Frequency (Hz)	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. X _d	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)
40501	500	400	21355	83.0	6452	58855	1.56	H-11	77.60	26.06	116400	66680
02	↓	1200	13000	86.8	6652	39499	0.99	↓	59.55	29.14	124900	58713
03		2000	14375	84.1	7112	48874	0.83		63.38	27.22	125000	66422
04		2800	19211	1.1	-	-	5.86		71.45	26.50	125000	49357
05	↓	3200	17899	77.2	6232	77898	1.35	↓	63.67	35.20	125000	71483
40801	800	400	20298	85.8	6912	42298	1.39	H-11	76.09	25.61	76500	67814
02	↓	1200	13186	88.6	5432	30186	1.14	↓	59.16	30.29	81900	59444
03		2000	13050	84.1	6132	38050	1.59		58.45	31.62	82000	59401
04		2800	25638	84.9	-	49638	5.7		75.18	32.17	82000	49995
05	↓	3200	17535	83.1	4872	44535	1.31	↓	63.99	33.99	82000	73175
40806	800	400	50544	75.6	10560	>95000	1.21	Nivco	105.9	28.49	76500	185565
07	↓	1200	20917	84.7	6632	44917	1.28	↓	73.09	27.37	81900	116918
08		2000	26815	83.3	5632	53615	1.03		82.96	25.01	82000	151361
09		2800	11018	2.3	-	-	5.06	↓	59.18	19.77	82000	96793
10	↓	3200		Design Failed								
41101	1100	400	50997	1.7	11140	187072	1.19	Nivco	107.0	27.83	15850	187072
02	↓	1200	20346	86.9	5962	117895	1.09	↓	73.69	25.77	17000	117895
03		2000	13992	3.0	5602	96690	0.68		66.97	21.93	17000	96690
04		2800	10755	0.8	-	96756	5.1	↓	58.81	20.40	17000	96756
05	↓	3200	15386	0.6	3422	101266	0.91	↓	67.66	24.10	17000	101266

Figure 2.1-1
 400 KVA Generator Frequency Study First Cycle Designs

FREQUENCY # VARIABLE; RPM = 15,000; L-N VOLTAGE = 1000

Figure 2.1-2

1200 KVA Generator Frequency Study First Cycle Designs

Figure 2.1-3
6 MVA Generator Frequency Study First Cycle Designs

Figure 2.1-4
12 MVA Generator Frequency Study First Cycle Designs

Figure 2.1-5
Creep Strength Versus Temperature For 0.2% Creep Strain
in 10,000 Hours

limit the temperatures to ranges compatible with chosen materials.* Sixty-seven of the eighty designs in the first cycle frequency study equalled or exceeded the 1300°F winding temperature limit of Inconel 600-clad silver wire with Anadur insulation (used in 500°F and 800°F designs) and the 1600°F winding temperature limit of dispersion-strengthened copper with alumina insulation (used on 1100°F designs). Therefore, the initially chosen a-c current density of 9000 amperes per square inch was reduced to 7000 amps/in² for subsequent 500°F designs and to 6000 amps/in² for subsequent 800 and 1100°F designs.

The use of four a-c conductors in parallel in the same slot resulted in the choice of a standard round wire by the computer design program for the smaller conductors required in the 400 and 1200 kva designs. A single rectangular conductor was specified instead of four round conductors in subsequent 400 and 1200 kva parametric designs to obtain better slot fill and better heat transfer characteristics.

A summary of the changes in chosen generator parameters and materials made as a result of the first cycle frequency study is shown in Table 2.1-5.

2. Speed Study - Following the frequency study, a speed study was conducted by calculating generator designs combining the following parameters and using the best frequencies from the frequency study.

Generator Rating (mva)	Coolant Temp. (°F)	Frequency (Hz)	Speed Range (RPM)
0.4	500	2000	12000 to 60000
0.4	800		12000 to 30000
0.4	1100		6000 to 15000
1.2	500		8000 to 40000
1.2	800		8000 to 20000
1.2	1100		4000 to 10000
6.0	500,800	1200	4500 to 18000
6.0	1100		2250 to 9000
12.0	500,800		2000 to 12000
12.0	1100		1000 to 6000

1000 volts line-to-neutral was used for all speed study designs.

*The computer program utilizes a fixed cooling configuration to analyze the winding temperatures. Although input data may be varied to circumvent normal routine and thereby account for variations in the cooling configuration, it was not done as part of the parametric screening studies in order to assess the relative severity of cooling problems associated with the various designs.

Table 2.1-5 Changes in Materials and Generator Parameters
Following First Cycle Frequency Study

Item	Initial Choice	Revised Choice	Reason for Change
Rotor material for 500°F and 800°F designs	H-11 or Nivco	H-11	Designs with Nivco rotors exceeded stress limits
Speed ranges to be studied for 1100°F designs:			
400 KVA	12000 to 30000 rpm	6000 to 15000 rpm	Stress limits or Nivco were exceeded at higher speeds.
1200 KVA	8000 to 20000 rpm	4000 to 10000 rpm	
6 MVA	4000 to 15000 rpm	2250 to 9000 rpm	
12 MVA	2000 to 12000 rpm	1000 to 6000 rpm	
Current Densities:			
500°F Designs	9000 a/in ²	7000 a/in ²	To reduce winding temperatures for compatibility with wire and insulation capabilities
800°F Designs	9000 a/in ²	6000 a/in ²	
1100°F Designs	9000 a/in ²	6000 a/in ²	
Conductors in parallel in same slot for 400 and 1200 KVA designs	4	1	To make feasible use of rectangular wire instead of round wire in the computer design iterations.

The speed study designs were again compared on the basis of generator electrical weight and on the basis of generator electrical weight plus estimated power plant and cooling system weight penalty.

Table 2.1-6 and Figure 2.1-6 show that for 500°F and 800°F coolant temperatures, 400 kva generator weight and system weight penalties tend to decrease up to speeds of 30000 rpm. Additional points calculated at speeds of 40000 and 60000 rpm for 500°F generators showed that little weight advantage was offered by these higher speeds; rotor material stress limits were exceeded at 60000 rpm. For the 1100°F, 400 kva designs, it was necessary to decrease the speed to 7500 rpm to maintain the rotor stress within the capabilities of Nivco. No suitable 6000 rpm design was obtained because of saturation and excessive excitation requirements encountered using the standard fixed parameters chosen as constants throughout the study. Based on these results, a speed of 30000 rpm for 500°F and 800°F coolants and a speed of 7500 rpm for 1100°F coolant were chosen for the 400 kva designs to be calculated in the voltage study.

Table 2.1-7 and Figure 2.1-7 show that for 500°F, speeds in the 24000 to 30000 rpm range provide the lowest 1200 kva generator and system weight, with little advantage gained by operating above 30000 rpm. Likewise it appeared that little weight advantage could be gained above 20000 rpm for the 800°F, 1200 kva designs. All 1100°F, 1200 kva designs failed stress above 5000 rpm and failed by only 10% at 5000 rpm. No suitable 4000 rpm, 1200 kva design was obtained because of saturation and excessive excitation requirements encountered using the standard design inputs. Based on these results, a speed of 24000 rpm for 500°F and 800°F coolants and a speed of 4800 rpm for an 1100°F coolant were chosen for the 1200 kva designs to be calculated in the voltage study.

Table 2.1-8 and Figure 2.1-8 show a 12000 to 14400 rpm speed range advantageous for 500°F and 800°F 6 mva designs; however, the 800°F designs failed stress at 14400 rpm. All 1100°F designs failed stress except those at the lower speed of 2250 rpm. A speed of 12000 rpm for 500°F and 800°F coolants and a speed of 2250 rpm for the 1100°F coolant were chosen for the 6 mva voltage study designs.

Table 2.1-9 and Figure 2.1-9 show the 8000 to 12000 rpm range advantageous for 500°F and 800°F 12 mva designs; however, the 800°F designs failed stress at 12000 rpm. The three 1100°F designs which were satisfactorily calculated electrically, all failed stress. Complete 1100°F designs at 1000 and 2000 rpm were not obtained because of saturation and excessive excitation requirements encountered using the standard design inputs. A speed of 10286 rpm (for 1200 cps with 14 poles) was chosen for 500°F and 800°F for the 12 mva voltage study. A speed of 1000 rpm was arbitrarily chosen for further 1100°F, 12 mva design trials to be run for the voltage study.

Whereas 67 of the 80 designs of the frequency study exceeded the temperature limits of the conductor insulations using an a-c current density of 9000 amps/in² as previously discussed, only 24 of the 64 designs of the speed study exceeded the temperature limits with the a-c current densities reduced to 7000 amps/in² for a 500°F

Table 2.1-6 Summary of 400 KVA Generator Designs, Speed Study, First Cycle,
2000 Hz, 1000 Volts L-N

Design No.	Average Coolant Temp. (°F)	RPM	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. Xd	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)
10510	500	12000	691	90.4	772	1501	0.89	H-11	23.49	12.99	125000	15880
09	↓	15000	563	88.6	920	1563	1.05	↓	22.37	12.30	124400	21330
08		20000	438	91.1	877	1178	1.01		20.27	11.84	123300	32644
07		24000	397	91.2	961	1137	0.91		19.50	11.62	122200	45016
06		30000	355	91.3	987	1085	0.93		18.66	11.50	120300	64091
11		40000	301	90.7	1172	1091	1.14		17.52	11.67	116400	98334
12	↓	60000	295	89.3	1559	1225	1.00	↓	16.86	11.98	105200	206829
10815	800	12000	635	89.8	1130	1375	1.05	H-11	23.17	12.04	82000	15714
14	↓	15000	563	88.9	1232	1363	1.04	↓	22.41	12.11	81600	21953
13		20000	443	91.1	1192	1073	1.00		20.41	11.80	81000	33213
12		24000	364	91.1	1267	994	1.09		19.06	11.36	80200	42930
11	↓	30000	363	91.4	1280	963	0.93	↓	18.85	11.51	79000	65456
11110	1100	6000	1134	Design Failed				Nivco				
09	↓	7500	1714	87.0	1282	2634	1.04	↓	32.07	13.67	17000	16999
08		10000	1229	88.0	1316	2079	1.04		29.10	12.75	17000	23651
07		12000	708	87.9	1390	1568	1.14		24.64	11.28	17000	23033
06	↓	15000	883	88.7	1362	1663	0.92	↓	26.08	12.17	16990	41559

Table 2.1-7 Summary of 1200 KVA Generator Designs, Speed Study, First Cycle
2000 CPS, 1000 Volts L-N

Design No.	Average Coolant Temp. (°F)	RPM	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. Xd	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)
20510	500	8000	2433	91.9	842	4153	0.98	H-11	32.84	20.20	125000	16002
09	↓	10000	1801	91.3	893	3701	1.24	↓	30.80	17.70	125000	20535
08		12000	1752	92.1	952	3252	0.95		30.07	18.01	125000	29395
07		15000	1271	92.2	1002	2921	1.16		28.13	15.79	124400	37067
06		20000	982	92.2	1182	2632	1.39		25.72	15.08	123300	56509
11		30000	870	92.1	1390	2540	1.09		24.51	15.08	120300	117621
12	↓	40000	755	92.1	1637	2455	1.22	↓	23.18	14.73	116400	183007
20815	800	8000	2472	91.9	1137	3892	0.99	H-11	33.26	19.93	82000	16548
14	↓	10000	1836	91.3	1202	3376	1.24	↓	31.28	17.45	82000	21129
13		12000	1656	92.3	1199	2996	0.91		29.78	17.68	82000	28293
12		15000	1189	92.0	1371	2589	1.29		28.15	14.52	81600	39447
11	↓	20000	998	92.3	1472	2338	1.38	↓	25.94	14.94	81000	57732
21110	1100	4000	Design Failed					Nivco				
09	↓	5000	7045	89.8	1260	8805	1.14	↓	46.86	22.67	17000	18848
08		6000	5136	90.3	1287	6816	1.24		43.74	19.52	17000	22779
07		7500	3679	89.6	1326	5479	1.34		41.28	16.18	17000	31247
06	↓	10000	2887	90.4	1327	4537	1.29	↓	36.59	17.24	17000	41556

Table 2.1-8 Summary of 6 MVA Generator Designs, Speed Study, First Cycle,
1200 CPS, 1000 Volts L-N

Design No.	Average Coolant Temp. (°F)	RPM	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. Xd	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)
30510	500	4500	15746	89.9	1062	25346	1.12	H-11	58.06	38.15	125000	17667
09	↓	9000	8318	92.2	1389	15318	1.16	↓	48.39	30.83	124400	48183
08		12000	6113	92.7	1782	12613	1.47		44.61	27.48	123300	72503
07		14400	5379	92.3	2862	12279	1.07		44.20	24.07	122200	100960
06	↓	18000	4289	90.4	5352	13289	1.27	↓	41.33	21.75	120300	141602
30815	800	4500	15962	90.4	1316	23542	1.13	H-11			82000	18250
14	↓	9000	8360	92.9	1551	13660	1.14	↓	49.00	30.21	81600	48932
13		12000	6140	93.2	2027	11340	1.45		45.09	26.96	81000	74080
12		14400	5919	93.0	2287	11119	1.14		45.43	25.22	80200	108687
11	↓	18000	4457	91.6	4362	10957	1.30	↓	41.81	22.41	79000	146211
31110	1100	2250	66141	81.4	1347	81942	0.99	Nivco	94.71	47.03	17000	16800
09	↓	4500	19512	90.5	1421	26712	0.66	↓	67.10	31.23	17000	30938
08		6000	12036	90.9	1693	18836	1.41		58.86	26.75	17000	40329
07		7200	9701	92.1	1654	15501	1.24		55.73	24.57	17000	52446
06	↓	9000	8361	92.1	1780	14161	1.36	↓	52.44	24.73	16990	70855

Table 2.1-9 Summary of 12 MVA Generator Designs, Speed Study, First Cycle,
1200 CPS, 1000 Volts L-N

Design No.	Average Coolant Temp. (°F)	RPM	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. X _d	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)
40510	500	2000	51447	85.2	-	83447	2.18	H-11	90.3	43.74	125000	7690
09	↓	4000	28358	89.2	1844	48358	0.92	↓	74.20	38.79	125000	23366
08		6000	20229	90.4	1740	37829	1.46		63.39	41.15	125000	39232
07		8000	13554	90.6	3742	30554	1.03		59.80	30.50	124900	59484
06	↓	12000	9769	90.7	5782	26769	1.08	↓	54.53	26.86	123300	109985
40815	800	2000	73478	76.8	-	123478	2.22	H-11	104.5	43.98	82000	8207
14	↓	4000	32792	90.7	1787	46392	1.01	↓	77.32	41.20	82000	25974
13		6000	20195	92.0	1972	31694	1.44		63.97	40.21	82000	40144
12		8000	14064	93.0	2652	23864	1.06		60.32	31.45	81900	61386
11	↓	12000	10760	92.4	3882	21660	1.15	↓	56.16	28.02	81000	119116
41110	1100	1000	Design Failed					Nivco				
09	↓	2000	Design Failed					↓				
08		3000	73543	86.4	1532	93543	1.47		101.5	46.23	17000	34761
07		4000	42104	89.5	1652	57004	1.48	↓	88.93	35.59	17000	44939
06	↓	6000	Design Failed

Figure 2.1-6
 400KVA Generator Speed Study First Cycle Designs

Figure 2.1-7
1200 KVA Generator Speed Study First Cycle Designs

Figure 2.1-8
 6 MVA Generator Speed Study Cycle Designs

Figure 2.1-9
12 MVA Generator Speed Study First Cycle Designs

coolant and 6000 amps/in² for 800 and 1100°F coolants. To further reduce the winding temperatures in subsequent designs, the assumption that stator slot voids are filled with a gas (with a thermal conductivity varying between 0.001 and 0.002 watts per inch °C) was changed to the assumption that the stator slot voids will be filled with a high temperature potting compound (with a thermal conductivity of not less than 0.015 watts per inch °C) for all designs.*

3. Voltage Study - Following the frequency and speed studies, a voltage study was conducted by calculating designs combining the following parameters and using the frequency and speed combinations found best from the foregoing studies.

<u>Generator Rating (mva)</u>	<u>Coolant Temp. (°F)</u>	<u>Frequency (Hz)</u>	<u>RPM</u>	<u>Voltage Points (Line-to- Neutral)</u>
0.4	500,800	2000	30000	
0.4	1100	2000	7500	500,1000,1500,&
1.2	500,800	2000	24000	2140 for all rat-
1.2	1100	2000	4800	ings & temperatures
6.0	500,800	1200	12000	plus additional
6.0	1100	1200	2250	250V designs for
12.0	500,800	1200	10286	6 mva, 500 & 800°F
12.0	1100	1200	1000	

In this study, not only the slot cell thickness varied with voltage but also the number of conductor turns in series varied. This latter varying parameter in turn varied the direct-axis synchronous reactance (X_d) for different combinations of design parameters. The following values of X_d were initially chosen to reasonably limit (1) voltage unbalance for unbalanced loads, (2) excitation power, (3) transients and (4) more use of field power for overloads:

<u>Rating (mva)</u>	<u>Desired Per Unit X_d</u>
0.4	1.0
1.2	1.2
6.0	1.3
12.0	1.3

*Further analysis of the applicability of this assumption to practical cases will be completed during the conceptual designs. Preliminary analyses (which even accounted for thermal expansion cracking) indicated it feasible from a theoretical standpoint.

Some light-weight designs were obtained with considerably higher X_d values than those shown above and some high-weight designs were obtained with considerably lower X_d values than those shown above. In order to choose designs with comparable performance, designs with X_d values varying more than 10 percent from the chosen values were not used in choosing the voltages which provided the best generator and system weight advantages.

As shown in Tables 2.1-10 through 2.1-12 and Figures 2.1-10 through 2.1-12, 500 volts results in generator and/or system weight advantages for the following designs:

400 kva, 500°F and 800°F
1200 kva, 1100°F
6 mva, 500°F, 800°F and 1100°F

The 400 kva, 1100°F design showed a generator electrical weight advantage at 1000 volts and about equal system weights at 500 and 1000 volts due in part to the higher weight of the lower X_d , 500 volt design; the 1200 kva, 500 volt, 500 and 800°F designs had low X_d 's and thus higher weights at 500 volts. Additional 250 volt designs calculated for 6 mva, 500 and 800°F, confirmed the advantage of 500 volts. Based on the trends shown by the designs calculated, 500 volts line-to-neutral was chosen for further design studies on 0.4, 1.2, and 6.0 mva designs.

As shown in Table 2.1-13 and Figure 2.1-13, the 12 mva designs showed a system weight advantage at 1000 volts even though the low X_d , 1000 volt designs had higher electrical weight than did the 500 volt designs. A voltage of 1000 volts was chosen for further design studies on 12 mva designs.

Twenty-one of fifty designs calculated for the voltage study exceeded the temperature limitations of the wire insulations; however, twelve of those twenty-one were 1500 or 2140 volt designs. The reduced current densities chosen following the frequency study and the increased thermal conductivity (for potting in the slots) used following the voltage study appear sufficient at this point in the program to maintain the temperatures at acceptable levels for 500 and 1000 volt designs in following studies.

4. Summary of First Cycle Generator Studies - Table 2.1-14 summarizes the frequencies, speeds, and voltages found to offer system weight advantages. These values will be used in further refining design studies.

Changes in fixed parameters and choice of materials recommended for use in further design studies, based on improvements found in the first cycle study, are:

(a) Reduction of a-c current densities to 7000 amps/in² for 500°F designs and to 6000 amps/in² for 800 and 1100°F designs instead of the 9000 amps/in² value initially chosen for all temperatures.

Table 2.1-10 Summary of 400 KVA Generator Designs, Voltage Study,
First Cycle, 2000 CPS

Design No.	Average Coolant Temp. (°F)	L-N Volts	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. Xd	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)	RPM
10513	500	500	346	91.7	822	1026	0.90	H-11	18.48	11.50	120300	65964	30000
14	↓	1000	355	91.5	837	1055	0.93	↓	18.66	11.50	↓	64091	↓
15		1500	335	90.4	942	1145	1.09		18.45	11.24		60994	
16	↓	2140	408	87.8	1147	1508	1.01	↓	19.99	12.16	↓	63805	↓
10816	800	500	349	90.9	1167	999	0.89	H-11	18.56	11.42	79000	67000	30000
17	↓	1000	363	90.7	1184	1023	0.93	↓	18.85	11.51	↓	65456	↓
18		1500	345	89.9	1290	1065	1.08		18.68	11.39		62119	
19	↓	2140	433	86.9	1522	1413	1.02	↓	20.45	12.54	↓	65560	↓
11111	1100	500	1879	88.5	1249	2689	0.88	Nivco	31.14	15.78	17000	17157	7500
12	↓	1000	1714	87.0	1280	2664	1.04	↓	32.07	13.67	↓	16999	↓
13		1500	1770	84.9	1282	2870	1.02		32.83	13.68		17342	
14	↓	2140	1956	84.3	1302	3136	0.99	↓	33.73	14.44	↓	17935	↓

Table 2.1-11 Summary of 1200 KVA Generator Designs, Voltage Study, First Cycle, 2000 CPS

Design No.	Average Coolant Temp. (°F)	L-N Volts	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. X_d	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)	RPM
20513	500	500	1049	92.6	902	2599	0.86	H-11	25.82	16.08	122200	84300	24000
14	↓	1000	889	92.7	960	2529	1.33	↓	24.81	14.87	↓	76457	↓
15	↓	1500	917	92.5	1016	2517	1.24	↓	25.24	14.50	↓	75846	↓
16	↓	2140	906	91.3	1212	2806	1.22	↓	25.60	13.75	↓	74712	↓
20816	800	500	1060	92.6	1247	2360	0.85	H-11	26.11	15.83	80200	85988	24000
17	↓	1000	911	92.5	1292	2211	1.32	↓	25.07	14.80	↓	78072	↓
18	↓	1500	919	92.6	1370	2219	1.21	↓	25.35	14.37	↓	76602	↓
19	↓	2140	1030	91.6	1370	2530	1.20	↓	26.30	14.97	↓	80527	↓
21111	1100	500	7129	85.4	1312	9829	1.46	Nivco	45.01	24.16	17000	16671	4800
12	↓	1000	7531	89.3	1350	9411	0.98	↓	45.64	24.72	↓	16640	↓
13	↓	1500	8305	88.2	1270	10405	1.14	↓	47.41	25.25	↓	17590	↓
14	↓	2140	9096	87.2	1270	11396	1.19	↓	48.23	26.86	↓	18290	↓

Table 2.1-12 Summary of 6 MVA Generator Designs, Voltage Study, First Cycle,
1200 CPS

Design No.	Average Coolant Temp. (°F)	L-N Volts	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. X _d	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)	RPM
30515	500	250	5561	77.6	6082	35661	0.83	H-11	47.64	18.75	123300	78629	12000
11	↓	500	5093	92.3	1861	12093	1.12	↓	44.60	21.44	↓	71587	↓
12		1000	6713	92.8	1223	13113	1.27		45.51	28.88		76910	
13		1500	6956	91.5	1647	14856	1.26		46.66	28.29		74487	
14	↓	2140	7241	92.2	1404	14240	1.23	↓	47.42	28.40	↓	76042	↓
30820	800	250	8606	82.8	3462	23607	1.23	H-11	53.73	22.44	81000	101969	12000
16	↓	500	5318	92.5	1852	11018	1.14	↓	45.20	22.04	↓	74571	↓
17		1000	6685	93.3	1524	11685	1.24		45.85	28.30		77837	
18		1500	7221	92.2	1757	13121	1.29		47.35	28.65		76953	
19	↓	2140	7310	92.8	1694	12510	1.21	↓	48.05	27.93	↓	77868	↓
31111	1100	500	19999	93.0	-	24999	1.84	Nivco	68.12	25.72	17000	13950	2250
12	↓	1000	66142	81.7	1337	81742	0.99	↓	94.71	47.03	↓	16800	↓
13		1500	75023	79.2	1382	93022	1.18		98.31	49.97		17977	
14	↓	2140	68590	80.6	1397	85290	1.28	↓	101.5	41.89	↓	18880	↓

Table 2.1-13 Summary of 12 MVA Generator Designs, Voltage Study, First Cycle, 1200 CPS

Design No.	Average Coolant Temp. (°F)	L-N Volts	Electrical Weight (lbs)	% Efficiency	Average Winding Temp. (°F)	Electrical Weight & System Weight Penalty (lbs)	P.U. X _d	Rotor Material	Maximum O.D. (in)	Electrical Length (in)	Maximum Allowable Rotor Stress (psi)	Actual Maximum Rotor Stress (psi)	RPM
40511 12 13 14	500 ↓	500 1000 1500 2140	9480 11895 13783 16453	87.2 93.3 93.1 92.0	5122 1817 1542 1726	34980 23395 25583 30453	1.44 0.98 1.23 1.07	H-11 ↓	54.69 56.87 56.55 59.27	25.63 30.15 36.27 38.94	124000 ↓	82499 89426 89766 95072	10286 ↓
40816 17 18 19	800 ↓	500 1000 1500 2140	9641 12011 13981 16540	88.1 93.8 93.5 92.7	4532 2012 1787 1942	28641 20711 23181 26840	1.43 0.97 1.21 1.06	H-11 ↓	55.39 57.53 57.42 60.01	25.22 27.64 35.52 38.16	81500 ↓	85014 91690 92518 97556	10286 ↓
41111 12 13 14	1100 ↓	500 1000 1500 2140	Design Design 765891 145005	Failed Failed 59.2 56.9	1563 -	>800000 >200000	0.84	Nivco ↓	223.7 120.3	82.62 85.36	17000 ↓	19936 5377	1000 ↓

Table 2.1-14 Summary of Results from First Cycle
Generator Design Studies

Rating (mva)	Coolant Temp. (°F)	Values Chosen for Subsequent Studies		
		Freq (Hz)	Speed (rpm)	Volts L-N
0.4	500 & 800	2000	30000	500
0.4	1100	2000	7500	500
1.2	500 & 800	2000	24000	500
1.2	1100	2000	4800	500
6.0	500 & 800	1200	12000	500
6.0	1100	1200	2250	500
12.0	500 & 800	1200	10000	1000
12.0	1100	1200	1000	1000

FREQUENCY=2000 Hz; RPM AS SHOWN BELOW; L-N VOLTAGE VARIABLE

Figure 2. 1-10

400 KVA Generator Voltage Study First Cycle Designs

FREQUENCY = 2000Hz; RPM # AS SHOWN BELOW;
L-N VOLTAGE # VARIABLE

Figure 2.1-11
1200 KVA Generator Voltage Study First Cycle Designs

Figure 2.1-12
6 MVA Generator Voltage Study First Cycle Designs

FREQUENCY = 1200Hz; RPM # AS SHOWN BELOW:
L-N VOLTAGE # VARIABLE

Figure 2.1-13

12 MVA Generator Voltage Study First Cycle Designs

(b) Use of H-11 rotor material instead of Nivco in designs for 800°F coolant temperatures.

(c) Reduction of speeds to 7500 rpm and lower in designs for 1100°F coolant temperatures to limit rotor stresses.

(d) Use of a single rectangular conductor instead of four round conductors in parallel in each stator slot of 400 and 1200 kva designs to obtain better slot fill in the computer configurations.

(e) Use of alternate experimental rotor materials for trial 1100°F designs. The majority of 1100°F designs in the first cycle studies equaled or exceeded the stress limits of Nivco rotor material.

B. Bearings, Seals, and Windage - Included in the generator computer design program are calculations for the bearing, shaft-seal and windage losses. Since these losses are included in the generator efficiency calculations, the bearings and seal designs specified must be based on operating conditions and requirements rather than on arbitrarily selected designs. A dilemma occurs, however, because the bearing and seal designs cannot be completed until such information as the rotor weight and speed are specified in the generator designs; thus, a first-cut trial using arbitrary bearing and seal designs is usually required to approximate the generator design from which more definitive bearing and seal designs may then be made. On a parametric study such as this, such cut and try methods are not practical and it becomes necessary to specify generalized generator design configurations upon which approximate bearing and seal designs may be based.

Since the generator design computer outputs from the previous 1-10 Megawatt Study were still available⁽¹⁾, a set of general design parameters (namely rotor weight and speed) could be specified for the parametric bearing and seal designs of this study. A composite of the rotor weights from the previous study are shown as a function of speed and electrical rating on Figure 2.1-14. The individual data points shown in addition to the four original data curves are the resulting rotor weights taken to date from the parametric output data of the present study. They are included to indicate the differences between the initially approximated rotor weights and the final rotor weights. Some of the weights on the present study are heavier than those of the previous study. The increases are due to different magnetic materials, changes in the computer program and the averaging of the data scatter of the original 1-10 Megawatt Study. The difference between the two sets of data, while certainly having an effect on the selected bearing designs, will not have a significant effect on the calculated generator efficiency and therefore, will not affect the overall conclusions drawn from the generator parametric study.

Figure 2.1-14

Generator Rotor Weight at Various Design Speeds

Four important computer input parameters which were calculated using the preliminary data on Figure 2.1-14 are:

Bearing bore (BB or D)
Stub shaft (seal) diameter (SD)
Friction coefficient (bearings and seals)
Density of gas in rotor cavity

The analyses to approximate these input parameters are summarized in the following sections. Refinements to these analyses were not carried out to optimize the resulting configurations since this will be done on the conceptual designs.

C. Bearing Bore and Friction - The bearings were pivoted pad journal bearings that were sized by considering the operating ranges and limits of certain dimensionless bearing characteristic numbers over the range of parameters applicable to the design study. Specifically, they were the:

1. Bearing characteristic number (S_p) which is equivalent to the more popular Sommerfield number with added parameters to account for variations in pad configurations.

$$S_p = k^2 C_w \left(\frac{R}{c} \right)^2 \frac{\mu n}{P}$$

k = fraction of bearing circumference filled with pads
 C_w = pad configuration aspect factor
 R = bearing radius, inches
 c = mean radial clearance, inches
 μ = fluid absolute viscosity, lb sec/in²
 n = shaft speed, rps (N = rpm)
 P = bearing pressure = W/LD , psi
 W = load on bearing, lbs
 L = bearing length, inches
 $D = 2R = BB$, inches

2. Taylor ratio which is a measure of the turbulence and consequently the losses in the bearing and is the ratio of the mean Reynolds number to the critical Taylor number; that is,

$$T_r = Re/T_a = \frac{\rho V c}{\mu g_c} \frac{41.1}{\sqrt{c/R}}$$

where V = velocity, in/sec
 ρ = density, lb/in³

3. The eccentricity ratio which is defined as

$$\epsilon = \frac{e}{c}$$

where e is the radial runout between the shaft centerline and the bearing centerline.

4. The pad aspect ratio B/L where

$$B/L = \frac{\pi Dk/n'}{L} = \frac{\pi k}{(L/D)n'}$$

and $n' =$ number of pads.

The following assumptions and constants were selected to establish the basis for approximating the bearing designs.

- a. Assume a 6-pad bearing with $k = 0.85$.
- b. Use $L/D = 0.75$ for 0.4 and 1.2 mva rating designs; use $L/D = 1.0$ for 6.0 and 12.0 mva ratings.*
- c. Consider the load to be directly over a pad - the condition of least load capacity - so that results per assumption "d" below are conservative.
- d. Assume dynamic loading corresponding to synchronous whirl about a radius "e" at all speeds, a worst case condition. Because of this assumption, magnetic loading was assumed to be negligible as well as unbalance loading.
- e. Limit ϵ to 0.95 maximum at 1/5 rated speed (startup film).
- f. Limit T_r to 10 or less at rated speed.

According to assumptions "e" and "c" and the design chart of Figure 2.1-15, the following limit is established.

$$S_p \geq 0.75 \text{ @ } 1/5 \text{ rated } N$$

Thus, $S_p \geq 3.75 \text{ @ rated } N$ which results in:**

$$\epsilon \leq 0.885$$

*Selected on the basis of preliminary design calculations.

**It was later recognized that a better assumption here might have been to limit ϵ to 0.9 at 1/5 speed which would have resulted in $\epsilon \leq 0.77$. Since the results will not have a significant effect on the generator performance, the "improved" assumption was not applied to the parametric study input data. Later conceptual designs will include this refinement.

According to assumptions "a" and "b" and the design charts of Figure 2.1-16, the following constants are established.

$$k^2 C_w = 162 \text{ at } L/D = 1.0$$

$$k^2 C_w = 180 \text{ at } L/D = 0.75$$

According to assumption "d", the maximum bearing load is:

$$W = \frac{W_{ROT} + W_{dynamic}}{2} = \frac{W_{ROT}}{2} + \frac{W_{ROT}}{2g_c} e (2\pi n)^2 = \frac{W_{ROT}}{2} \left(1 + \frac{C_c (2\pi n)^2}{g_c} \right)$$

Combining the above bearing load with the bearing characteristic number gives:

$$S_p = \frac{k^2 C_w \mu n^2 L D}{W_{ROT} \left(1 + \frac{C_c (2\pi n)^2}{g_c} \right)} = \frac{4k^2 C_w (R/c)^3 \mu n c (L/D)}{W_{ROT} \left(1 + \frac{C_c (2\pi n)^2}{g_c} \right)}$$

or, since $S_p \geq 3.75$,

$$(R/c)^3 \geq \frac{(3.75)(W_{ROT}) \left(1 + \frac{C_c (2\pi n)^2}{g_c} \right)}{4k^2 C_w \mu n c (L/D)} \quad (2.1-1)$$

Now,

$$T_r = \frac{\rho V C}{\mu g_c} \frac{\sqrt{C/R}}{41.1} \leq T_r \text{ max.}$$

$$\text{Thus, } R/c \leq \left(\frac{41.1 T_r \text{ max } \mu g_c}{\rho C^2 2\pi n} \right)^2 \quad (2.1-2)$$

The two equations 2.1-1 and 2.1-2 thus define the limits of radius-to-clearance ratio for the design study and represent two equations with two unknowns, R and c. By equating the two equations and solving graphically (with T_r nominally equal to 6.3) the minimum bearing bores (BB) and minimum R/c ratios were determined; the results are shown on Figures 2.1-17 and 2.1-18. Note that for the 6.0 and 12.0 mva ratings, the resulting values of R/c are higher than what would normally be considered an acceptable value (i.e., clearance is too small). Some adjustments would have to be made in any final designs to lower the ratios to the order of 1000 to 1500 as on the 0.4 and 1.2 mva ratings. These adjustments might even include respecifying the bearing configuration to that of a hydrostatic

Figure 2. 1-15

Chart for Determining Eccentricity Ratio

Figure 2.1-16

Charts for Determining Aspect Factors

Figure 2.1-17

Minimum Bearing Bore (BB) as a Function of Design Speed N

Figure 2.1-18

Minimum R/c Ratios as a Function of Design Speed N

type which would offer more stiffness and load carrying capability for the low-speed, large ratings.

The configurations shown were not analyzed relative to optimizing the damping coefficients and spring coefficients. As a result, conclusions cannot be drawn at this time as to the stability behavior, critical damping, etc. of the rotor-bearing systems. To do so now would have required more effort than is warranted on the parametric screening studies. Also, it will be shown later in this section that the stability problems are not too severe for the generator alone since the operating speed is usually well below the first rotor-bending critical speed. Thus, the only critical conditions that must be considered in reaching the operating speed are the resonances associated with the bearing film spring stiffness.

Because the clearances are so small for all four ratings, special attention would have to be paid to assuring that self-loading would not occur in actual operating configurations. For example, Thermoflex bearing mounts would have to be used to compensate for differential thermal expansion in the radial clearance.

The bearing coefficient of friction was determined using the data of Figure 2.1-19 and the bearing dimensions previously calculated. However, the bearing characteristic was calculated using W equal to the static load only in this case. This results in a more conservative answer for bearing losses.

D. Stub Shaft (Seal) Diameter and Seal Friction - The minimum stub shaft diameter (SD) was determined using the configuration shown on Figure 2.1-20. The following assumptions were made to simplify the analysis.

$$L_b = 0.55L$$

$$SD \geq BB = D$$

$$\Delta \leq c$$

$$L_s = 1.2 \text{ SD for visco seal configurations on the 0.4 and 1.2 mva ratings.}$$

$$L_s = 3.5 \text{ inches for dynamic seal configurations on the 6.0 and 12.0 mva ratings.}$$

The latter assumptions are based on preliminary analyses which lead to the conclusion that, for the higher speed smaller ratings, visco-seal shaft seal configurations would be appropriate and, for the lower speed larger ratings, dynamic slingers are more appropriate. In order to make the dynamic slinger losses competitive with visco-seals during the analysis, the slinger configuration was assumed to have several stages, each approximately 0.5 inch in length.

Figure 2. 1-19

Chart for Determining Coefficient of Friction

Figure 2.1-20
Stub Shaft Configuration

The deflection was first approximated for the special case of SD = BB = D. Thus,

$$\begin{aligned} c \geq \Delta &\approx \frac{W(L_s + L_b)^3}{3E\left(\frac{\pi}{64} D^4\right)} \\ &= \frac{64W(1.2 + 0.55 L/D)^3}{3\pi ED} \quad \text{for 0.4 and 1.2 mva ratings} \\ &= \frac{64W(3.5 + 0.55L)^3}{3\pi ED^4} \quad \text{for 6.0 and 12.0 mva ratings} \end{aligned}$$

Upon substitution of appropriate values and solving for the maximum speed that gives $\Delta \leq c$, it was concluded that there is sufficient stiffness for the special case where the stub shaft diameter (SD) equals the bearing bore (BB); thus, further analyses for SD > BB were not made. The limiting speeds for this conclusion are shown on Figure 2.1-17 and are generally higher than the maximum generator design speeds discussed in Section 2.1.1 A. Because of this and the critical speed result below, a general rule was established for the generator parametric study whereby SD = BB = D.

The first critical speeds of the rotor/stub shaft configurations defined above may be approximated by considering the rotor mass to be infinitely stiff compared to the stub shafts and that there is no damping. The first bending critical is then,

$$\omega_f \approx \frac{\sqrt{k_s g_c}}{W} \quad \text{where } k_s = \frac{W}{\Delta_{\text{static}}} \quad (\text{lb/in}) \quad \text{and } \omega_f \text{ is in radius per second.}$$

Therefore,

$$N_f = \frac{187.7}{\sqrt{\Delta_{\text{static}}}}$$

where Δ_{static} is calculated with the above equations for $W = 1/2W_{\text{ROT}}$. The results are shown on Figure 2.1-21. Reference to this figure and the selected generator speeds of the previous section reveals that the generator will always be operating well below the first rotor-bending critical speed.

The friction losses for the seals were determined using the methods of References 2, 3 and 4 for the viscoseals on the 0.4 and 1.2 mva ratings and the method of Reference 5 for the dynamic seals on the 6.0 and 12.0 mva ratings. The following assumptions were used as guidelines for the analyses.

- a. Seal design pressure differential = 30 psi.
- b. The seal must be capable of sealing this pressure at 1/3-rated speed (startup condition).

Figure 2.1-21
First Resonant Speeds for Selected Rotor/Bearing Stub Shaft Configurations

- c. On the viscoseals, double threaded configurations* would be used with a physical radial clearance $c' = 0.002 \text{ SD}$; thus, the effective radial clearance $S = c'/2 = 0.001 \text{ SD}$.
- d. On the dynamic slingers, the tip (Do) to hub (SD) ratio would be 1.4 and 7 stages** would be used. All stages would be utilized at 1/3 speed and only the last stage would be utilized at rated speed. The impeller configurations are smooth on the flooded side and vaned on the interface side. The pressure rise coefficients are 0.1 and 0.9 for the smooth and vaned sides respectively.

The losses in the viscoseal are a function of the pressure being sealed. The pressure head per inch of flooded seal is (3),

$$\text{psi/inch} = 1.845 \times 10^{-4} \frac{\mu^2}{\rho s^3} \left(\frac{\rho V s}{\mu g_c} \right)^{2.18}$$

The viscoseal losses are calculated in accordance with journal bearing theory with modifications to account for the threads. The resulting losses per inch of flooded seal are (2), (4)

$$h_p/\text{inch} = 9.63 \times 10^{-11} \frac{\mu D^3 N^2}{s} \left(\frac{\rho V s}{\mu g_c} \right)^{0.57}$$

or in terms of the pressure being sealed,

$$h_p/\text{psi} = 3850 \times 10^{-6} \frac{D^2 s N}{\left(\frac{\rho V s}{\mu g_c} \right)^{0.61}} \quad (2.1-3)$$

The losses in the dynamic slinger seal may also be calculated as a function of the pressure head being sealed. The sealed pressure head in the last stage is, for the fluid vapor interface radius at the recommended 0.8 of the tip radius (5),

$$\text{psi} = \{0.9 [(1.4D/2)^2 - (0.8 \times 1.4D/2)^2] - 0.1 [(1.4D/2)^2 - (D/2)^2]\} \frac{\rho w^2}{2g_c} = 0.136 D^2 \frac{\rho w^2}{2g_c}$$

* Threads on shaft and housing both.

** Preliminary analysis indicated that, with this combination of parameters a reasonably compact, low power seal could be had. It also indicated that little improvement could be had by going beyond 7 to 9 stages.

The losses at this condition are⁽⁵⁾,

$$h_p = \frac{2\pi t N}{396000} = \frac{2\pi N C_m}{396000} \frac{\rho w^2}{2g_c} (1.4 D/2)^5$$

so that

$$h_p/\psi = 19.6 \times 10^{-6} D^3 N C_m$$

where C_m is the torque coefficient and at an interface ratio of 0.8 is approximately,

$$C_m \approx 1.063 \left(\frac{\rho V_o D_o}{2\mu g_c} \right)^{-0.25} = \frac{1.07}{\left(\frac{\rho V D}{\mu g_c} \right)^{0.25}}$$

Thus,

$$h_p/\psi = 21 \times 10^{-6} \frac{D^3 N}{\left(\frac{\rho V D}{\mu g_c} \right)^{0.25}} \quad (2.1-4)$$

Comparison of the two equations, 2.1-3 and 2.1-4, reveals the inherent power advantage the viscoseal holds over the dynamic slinger; that is

$$\begin{aligned} \frac{(h_p/\psi)_{\text{dynamic}}}{(h_p/\psi)_{\text{viscoseal}}} &= \frac{21 \times 10^{-6} D^3 N \left(\frac{\rho V s}{\mu g_c} \right)^{0.61}}{3850 \times 10^{-6} D^2 s N \left(\frac{\rho V D}{\mu g_c} \right)^{0.25}} \\ &= 0.00545 \frac{D^{0.75}}{s^{0.39}} \left(\frac{\rho V}{\mu g_c} \right)^{0.39} = 0.081 D^{0.36} \left(\frac{\rho V}{\mu g_c} \right)^{0.39} \\ &\text{for } s = 0.001 D \end{aligned}$$

which is a number always much greater than one (50 to 100) at the conditions being considered.

The seal losses are included in the generator computer design by inputting the friction coefficient corresponding to the above losses using the relations (2), (4),

$$\text{friction coefficient} = \frac{0.082}{Re^{0.43}}$$

and,

$$\frac{2 \text{ (torque)}}{\pi^3 \rho N^2 D^4 L} = \frac{1}{Re}$$

where

$$\text{torque} = \frac{(396000) (h_p)}{2\pi N} \quad (\text{in-lb})$$

The resulting bearing losses, seal losses and windage losses (see next section) for the optimum first cycle designs are usually of the order of 0.2 to 2 percent of the electrical rating for the 0.4-1.2 mva and 6-12 mva sizes respectively.

E. Density in Rotor Cavity - In order to calculate the windage losses in the rotor cavity, it is necessary (among other things) to specify the static density of the gas surrounding the rotor. Since Holweck type molecular vacuum pumps will be utilized to reduce the pressure (and hence the density and windage) it becomes necessary to establish some generalized pump designs and corresponding performance upon which the reduced density may be based. The basic shaft seal configuration utilized is based on past SNAP-50/SPUR designs and is shown on Figure 2.1-22. The configuration is a visco-seal with an over-hung Holweck pump. The static rotor cavity density may be determined as a function of the static saturated vapor properties and the pressure differential across the pump. That is,

$$\begin{aligned} \rho_{\text{static}} &= P_{\text{static}} (\rho/P)_{\text{base}} \\ &= \left(P_{\text{base}} + \frac{\rho_{\text{base}} V^2}{2g_c} \right) (\rho/P)_{\text{fore}} \\ &= (\rho/P)_{\text{sat}} \left(P_{\text{sat}} - \Delta P_H - \frac{V^2}{2g_c} \{ \rho_{\text{fore}} - \rho_{\text{base}} \} \right) \\ &= \rho_{\text{sat}} \left(1 - \frac{\Delta P_H}{P_{\text{sat}}} \left\{ 1 + \frac{0.0074}{T} \left(\frac{D_{HN}}{1000} \right)^2 \right\} \right) \end{aligned}$$

where

$$\begin{aligned} \Delta P_H &= \text{pressure difference across pump, psi} \\ D_H &= \text{Holweck Pump diameter, inches} \\ T &= \text{Absolute temperature, } ^\circ\text{R} \end{aligned}$$

Thus, we see that it is convenient to describe the generalized Holweck Pump performance (ΔP_H) in terms of pump diameter, shaft speed and coolant temperature.

The 500°F condition is comparable to the coolant temperature on previous SNAP-50/SPUR studies and during these studies it was found that a 10:1 pressure ratio could easily be maintained across the pump. Thus, at 500°F, it was assumed that:

$$\Delta P_H \approx 0.9 P_{\text{sat}} \text{ at all speeds.}$$

Figure 2.1-22

Basic Shaft Seal Configuration

* Given low pressure sink conditions, saturated vapor pressure, and density.

** Pressure difference across Holweck pump = $\Delta p_H = p_{fore} - p_{base}$

At 800 and 1100°F, the saturated vapor pressure increases, the pumps are not as efficient and pressure ratios less than 10:1 are more likely. In order to assess the performance at 800 and 1100°F, typical Holweck pump designs were made and their performance calculated(2); the results are shown on Figures 2.1-23 and 2.1-24. With these data, the rotor cavity densities as shown on Figure 2.1-25 were calculated.

There is very little variation in the 1100°F cases because the saturated vapor pressure is sufficient to prevent the pump from achieving any significant pressure ratios. In fact, most were 1.05:1 or less for the designs used; with design refinements, this could be improved but probably not much more than 2:1. At 800°F, the pressure ratios are of the order of 2:1 for the approximating designs used.

2.1.2 Plans for Coming Quarter

A. Second Cycle Generator Design Study - Frequency, speed and voltage studies will be conducted similar to those carried out during the first cycle study except using ranges of parameters near those which were found best during the first cycle studies, as shown in Table 2.1-14.

B. High Temperature Designs - Two designs having hot spot temperatures in excess of 1100°F will be chosen from the foregoing studies, analyzed, and further optimized in terms of weight and efficiency by varying internal design parameters. Alternate high temperature materials will be considered.

One design using 1400°F coolant will be evolved and optimized in terms of weight and efficiency by varying internal design parameters and materials.

C. Conceptual Generator Designs - Four configurations over the range of generator ratings studied will be chosen and further optimized in terms of weight and efficiency by varying internal design parameters. A layout drawing will be made and performance data will be tabulated for each configuration.

Figure 2. 1-23

Typical Holweck Pump Performance in 800°F Potassium

Figure 2. 1-24

Typical Holweck Pump Performance in 1100°F Potassium

Figure 2. 1-25

Density of Potassium Vapor Surrounding Rotor

2.2 AC Motor Parametric Study

ABSTRACT

Several motor design parameters that have been revised since the last quarter are defined. Several motor design parameters not defined in the last quarterly report are discussed. The 500°F, 800°F, and 1100°F coolant first cycle frequency studies were completed and the 500°F coolant first cycle speed study was completed. The most favorable design frequencies appear to be 400, 600, and 800 cps. The frequency of 400 cps was chosen for the 500°F first cycle speed study. The 200 cps, 2-pole designs are being eliminated from future consideration in the parametric study.

2.2.1 New and Revised Definitions of Motor Design Parameters or Data

Several motor design parameters listed but not defined in the last quarterly report have since been established. Also, in the course of obtaining the parametric data, it has become desirable to alter certain initial design parameters and design data. The need for these changes has been indicated by both the results of the design analyses and the desire to ease the task of obtaining the parametric data as much as possible. These new definitions and changes are listed and discussed in the following sections.

A. Revised Motor Design Parameters and Data

1. Speed - The motor design synchronous speeds to be investigated in the parametric study were originally defined to be 4000, 8000, 10000, 15000, and 20000 rpm. When the speed study portions of the parametric study were undertaken, it became apparent that investigation of additional synchronous speed points was desirable.

The reason for this change can be seen by considering the basic motor design relation,

$$N = \frac{120 f}{P}$$

where N = synchronous speed (rpm)
 f = frequency (cycles/second)
 P = number of poles

The number of poles should be an even integer (i.e., 2, 4, 6, 8, etc.). Specification of either of the two remaining variables, speed and frequency uniquely determines the value of the remaining variable. From the above equation it is clear that for a range of poles and with the frequency equal to any given value defined in Section 3.2.2(a) of the First Quarterly Technical Progress Report (26), (200, 400, 600, 800, 1000, 2000 cps), all the synchronous speed

values will not correspond to the speeds initially contemplated for the parametric study. Conversely, if a range of speeds are selected which conform to those initially defined, the required frequencies will not conform (in all cases) to the frequency points initially defined.

Of the two design parameters - speed and frequency - frequency is the most significant with respect to system electrical parametric considerations. Frequency is directly related to basic frequency changer and/or generator electrical design parameters while motor speed mainly concerns pump capacity (flow rate). Therefore, it was decided to maintain the initial parametric values of frequency which conveniently coincide with other system component design data. For any frequency determined in the frequency studies, the number of poles (2, 4, 6, 8...) will be chosen so that the range of fine speed points fall approximately in the speed range (4000-20000 rpm) specified initially.

Table 2.2-1 summarizes the possible synchronous speeds for the combinations of frequencies and number of poles to be investigated. The shaded blocks represent the choices of speed study for each frequency. The parametric data obtained to date indicates that, as the number of poles increases beyond ten, the designs tend to become progressively more unattractive. Therefore, it appears unlikely that designs at speeds much below 10000 rpm will be achieved at the higher frequencies (1000 to 2000 cps).

It is possible that some of the higher speeds indicated may prove unfeasible because of excessive long term deformation (creep) of the rotor can or of the punching teeth and/or because of excessive centrifugal loading of the rotor lamination teeth by the rotor conductor bars. No attempt has been made to establish an upper speed limit because each design requires a complex stress analysis involving variations in the relative dimensions of,

- a. rotor lamination teeth
- b. rotor conductor bars
- c. rotor can wall thickness
- d. rotor lamination diameters
- e. shaft wall thickness

and variations in,

- a. speed
- b. overall temperature level (coolant temperature)
- c. relative thermal expansion
- d. material properties

An example of this analysis may be found in the appendices of Reference 27.

Table 2.2-1 Synchronized Speeds for Various Combinations of Poles and Frequencies

Poles/ Frequency	2	4	6	8	10	12	14
200	12000	6000	4000				
400	24000	12000	8000	6000	4800	4000	
600	36000	18000	12000	9000	7200	6000	5143
800	48000	24000	16000	12000	9600	8000	6857
1000	60000	30000	20000	15000	12000	10000	8571
2000			40000	30000	24000	20000	17143
Poles/ Frequency	16	18	20	24	26	28	30
600	4500	4000					
800	6000	5333	4800	4000			
1000	7500	6667	6000	5000	4615	4286	4000
2000	15000	13333	12000	10000	9231	8571	8000

It is also possible that maximum speeds may be dictated by limits of practical bearing design.

2. Stator Slot Clearance - Since the last reporting period, further consultation with manufacturing personnel concerning practical tolerances and allowances for clearance based on experience in fabricating the 467 kva Experimental Generator components produced the following recommended changes in the allowance for clearance between the indicated stator slot components:

RADIAL

Initial Allowances

0.001 inch per conductor
+ 0.002 inch

Revised Allowances

0.001 inch per conductor

CIRCUMFERENTIAL

Initial Allowances

0.005 inch

Revised Allowances

Clearance between conductor
and slot lines =
0.001 x 2 sides = 0.002
Clearance between slot liner
and punching tooth =
0.001 x 2 sides = 0.002

Total 0.004 inch

The revised allowances were adapted and have been used in the parametric study to date. It should be noted that the actual stator and rotor punching slot dimensions not only reflect the allowances for clearances such as those discussed above but also reflect allowances for the tolerances on the various slot components, the punchings, and punching misalignment (called punching stagger). The latter tolerances have remained as reported in the last quarterly technical progress report.

B. New Motor Design Parameters

1. Rotor Can Thickness - (Refer to Section 3.2.2(a) (4), page 66, Quarterly Progress Report No. 1) (26) - A simple, brief analysis was made to roughly determine the rotor can thickness requirements as a function of rotor diameter, speed, and temperature. The criterion used was the limitation of rotor can deformation (creep) to less than 1 percent in 40000 hours since comparison between stress rupture strength and creep strength from available data on tantalum T-111 alloy (the rotor can material) disclosed creep strength to be the governing factor.

The most significant stress in the rotor can is the tangential stress component at the ID of the can; therefore, the analysis was limited to the tangential stress component. The tangential stress component was considered the sum of two parts, each resulting from different causes, (1) interference fit between the rotor can and rotor stack OD and (2) centrifugal loading (body forces), i.e.,

$$\sigma_t = \sigma_{tp} + \sigma_{tn} \quad (2.2-1)$$

where σ_t = total tangential stress component (psi)
 σ_{tp} = tangential stress from interference fit (psi)
 σ_{tn} = tangential stress from rotation (psi)

Since the rotor can thickness is small compared to the rotor can diameter, the stress can be assumed uniformly distributed across the wall and the approximate "hoop-stress" equations apply.

$$\sigma_{tp} = \frac{pc D}{2t} \quad (2.2-2)$$

where pc = interface contact pressure from interference fit (psi)
 D = mean diameter of rotor can (in)
 t = rotor can wall thickness (in)
 σ_{tp} = tangential stress from interference fit (psi)

and,

$$\sigma_{tn} = \frac{\gamma w^2 D^2}{4g} \quad (2.2-3)$$

where γ = specific weight of rotor can material (lbs/in³)
 w = angular velocity, rad/sec
 D = mean diameter of rotor can (in)
 g = gravitational constant
 σ_{tn} = tangential stress from rotation (psi)

If an appropriate design stress, σ_d , is substituted for σ_t in equation 2.2-1 and if expressions for σ_{tp} and σ_{tn} given by equations 2.2-2 and 2.2-3, respectively, are substituted into equation 2.2-1, rotor can thickness, t , can be determined.

The design stress is given by,

$$\sigma_d = \frac{\sigma_c}{1.5} \quad (2.2-4)$$

where σ_c = creep strength of Tantalum T-111 alloy at temperature and time of interest necessary to limit deformation to 1 percent.
1.5 = design factor to lend some conservatism to the analysis because of its approximate nature and to allow for unknown design conditions such as shock, vibration, stress concentration, and inaccuracy in material property data.

The allowable creep strength, σ_c , was taken from the Larson-Miller plot of vacuum creep and rupture data (Figure 2.2-1) for the appropriate rotor can temperatures (assumed as coolant temperature ($^{\circ}\text{F}$) $+100^{\circ}\text{F}$) and 40000 hours.

The resulting computations, based on an assumed interface pressure of 50 psi showed that for most of the diameter, speed and temperature combinations to be investigated in the parametric study, required rotor can thicknesses, t , was much less than 0.010 inch. At about the 1100°F , 15000 rpm, 7.5 inch diameter point, the required can thickness begins to increase rapidly. However, as creep deformation takes place, the interference fit will relax causing the interface pressure to decrease which in turn decreases the tangential stress. The can may then carry larger amounts of centrifugal loading without exceeding the permissible creep strength level.

Fabrication considerations take on increasing importance when the required thicknesses for mechanical strength are so small (≤ 0.010 inches). Since it appears feasible to deep draw rotor cans with 0.010 to 0.015 inch walls; it was decided to specify a rotor can thickness of 0.015 inches for the entire range of rotor diameters, speeds, and temperatures contemplated for the parametric study.

2. Running Air Gap - (Refer to Section 3.2.2(a)(4)c, Quarterly Technical Progress Report No. 1) (26) - An empirical relation incorporated into the motor computer program has been used to determine the "running" radial clearance between the rotor can OD and the bore seal ID for all ratings in all studies. This equation has proved satisfactory in past motor design and fabrication at Westinghouse Aerospace Electrical Division and is considered compatible with the objectives of the present parametric study. The relation is,

Figure 2.2-1

Larson-Miller Plot of Vacuum Creep and Rupture Data for Several Columbium and Tantalum Base Alloys 0.012 Inch Thick Samples. (Reference: NASA-CR-54093, Bore Seal Technology Report, December 1964)

$$\text{RUN} = 0.005 + \frac{0.0042 (\text{ID})}{\sqrt{p}} \quad (2.2-5)$$

where RUN = running radial clearance (inches)
 ID = stator ID (inches)
 P = number of poles

For the parametric designs obtained to date, the running radial clearance has varied between 0.010 and 0.020 inch.

3. Shaft Diameter - (Refer to Section 3.2.2(a) (6), Quarterly Technical Progress Report No. 1) ⁽²⁶⁾ - Shaft diameters for each study were determined on the basis of two requirements, (1) adequate torque transmitting capacity and (2) adequate central cooling passage size (shaft ID).

(a) Cooling Passage Size (Shaft ID) - The shaft ID must be large enough so that, (1) sufficient heat transfer surface area is provided to reject rotor losses to the coolant without a prohibitive temperature drop across the coolant film (which would cause a large ΔT between rotor conductor bar and coolant) and (2) the coolant flow velocity is held below practical limitations. The approximate shaft ID was determined from the following rather simplified approach.

The governing heat transfer equation at the shaft ID is,

$$q = h \frac{A}{144} \Delta T \quad (2.2-6)$$

$$\text{or, } A = \frac{144 \text{ g}}{h \Delta T} \quad (2.2-7)$$

where

- q = rotor losses or heat load per unit time (BTU/hr)
- h = coolant connective heat transfer coefficient (BTU/hr-ft²-°F)
- ΔT = temperature difference across coolant film (°F)
- A = heat transfer surface area at shaft ID (in²)

but,

$$A = \pi D_1 L \quad (2.2-8)$$

where D_1 = shaft ID (in)
 L = rotor stack length (in)

Substituting equation 2.2-8 into equation 2.2-7 and rearranging,

$$D_1 = \frac{144 \text{ } q}{\pi L h \Delta T} \quad (2.2-9)$$

The coolant flow velocity is determined from,

$$V = \frac{Q}{A} = \frac{576 \text{ } Q}{\pi D_1^2} \quad (2.2-10)$$

and,

$$D_1 = \sqrt{\frac{576 \text{ } Q}{\pi V}} \quad (2.2-11)$$

where Q = coolant flow rate (ft³/sec)
 V = coolant velocity (ft/sec)
 D_1 = shaft ID (in)

Based on previous liquid metal cooled generator experience, coolant velocity, V , was assumed limited to 50 ft/sec. Coolant flow rate, Q , was assumed to be 10 gallons per minute. Equation 2.2-11 then gives a minimum shaft ID = 0.244 inch. Further analysis showed that for all ratings (except for most 5 horsepower designs) equation 2.2-9 was the governing equation for shaft ID.

(b) Torsional Strength (Shaft OD) - The required shaft OD was determined from,

$$\tau_d = \frac{16 \text{ } T \text{ } D_2}{\pi (D_2^4 - D_1^4)} \quad (2.2-12)$$

$$= \frac{16T}{\pi D_2^3} \frac{1}{1 - K^4} \quad (2.2-13)$$

where τ_d = design stress (psi)
 T = torsional moment or torque (lb-in)
 D_2 = shaft OD (in)
 D_1 = shaft ID (in)
 $K = D_1/D_2$

The design stress was obtained from,

$$\tau_d = \frac{\tau_{yp}}{2.5} \quad (2.2-14)$$

where τ_{yp} = yield strength in shear of Tantalum T-111
 2.5 = design factor to allow for added lateral stiffness
 (since shaft deflection is being ignored for the
 present), fatigue, stress concentrations, etc.

The torsional moment of torque, T, was taken as the maximum deliverable torque for each rating at each speed; i.e.,

$$T = T_{\max} = \frac{(2.25)(63000)P}{N} \quad (2.2-15)$$

where P = rated motor power (horsepower)
 N = motor speed (RPM)
 T = torque (lb-in)

Shaft OD was determined from equation 2.2-12 using values of D_1 from equation 2.2-9. The results of these computations showed that K (equation 2.2-13) = 0.875 was more than adequate for all cases. Thereafter, once D_1 was determined, D_2 was determined from,

$$D_2 = \frac{D_1}{0.875} \quad (2.2-16)$$

4. Minimum Bending Radius of Stator End Extensions Hair-Pin Turns - (Refer to Section 3.2.2(a) (7), Quarterly Technical Progress Report No. 1) (26) - The following guidelines were used to determine the hair pin bending radii throughout the parametric study:

Estimated Bend Radii Based on Conductor Dimension

BEND RADII

(where X = conductor dimension measured in radial direction)

Conductor Width To Thickness Ratio	Winding Direction	
	Edge-wise	Flat-wise
1 : 1	see Note 3	see Note 3
1.5 : 1	1.5X	1.5X
2 : 1	1.5X	1.5X
2.5 : 1	2 X	1.5X
3 : 1	2 X	2 X

Notes:

- 1) Recommended maximum conductor dimension is approximately 0.3 inch for Anadur insulation on typical clad conductors.

- 2) Bend radius is measured to the conductor excluding the insulation.
- 3) Conductor width-to-thickness ratio of 1 : 1 is not recommended for pre-formed coil applications because of conductor twisting problems.

5. Windage Losses - (Refer to 3.2.2(a) (9), Quarterly Technical Progress Report No. 1) (26) - The total motor windage losses were considered to consist of two parts,

- a. rotor end losses
- b. losses in the annulus between the rotor can and the stator bore seal.

Since the "canned" rotor surfaces will be smooth, the total losses for both ends can be considered equivalent to the power absorbed on both sides of a submerged rotating disk. These losses are given by,

$$P = K \rho w^3 r^5 \quad (2.2-17)$$

where $K = 9.2 \text{ Re}_r^{-0.615}, \text{ Re}_r < 10^5 \quad (2.2-18)$

$$K = 0.0525 \text{ Re}_r^{-0.167}, \text{ Re}_r \geq 10^5 \quad (2.2-19)$$

$$\text{Re}_r = \text{Reynolds Number} = \frac{wr^2}{\nu} \quad (2.2-20)$$

ρ = mass density of fluid (slugs/in³)

w = angular velocity (radius/sec)

r = radius of rotor (in)

ν = Kinematic viscosity of fluid (in²/sec)

P = losses (lbs-in/sec)

The losses in the annulus between the rotor can OD and the stator bore seal are given by,

$$P = \rho w^3 r^5 \left(\frac{\pi L}{4r} \right) f \quad (2.2-21)$$

where L = length of annulus = rotor length (in)
 f = frictional coefficient

$$f = \frac{8}{\text{Re}_p} = \frac{8}{\text{Ta}} \sqrt{\frac{b}{r}}, \quad (\text{Ta} \leq 41.1) \quad (2.2-22)$$

$$f = (0.31) \sqrt{\frac{b}{r}} Ta^{-0.123}, \quad (41.1 < Ta \leq 90) \quad (2.2-23)$$

$$f = (1.52) \sqrt{\frac{b}{r}} Ta^{-0.478}, \quad (Ta > 90) \quad (2.2-24)$$

where $Ta = \text{Taylor's number} = Re_b \sqrt{\frac{b}{r}} \quad (2.2-25)$

$$Re_b = \text{Reynolds number} = \frac{wrb}{\nu} \quad (2.2-26)$$

$b =$ running radial clearance between rotor can OD
and stator bore seal ID (in)

6. Friction Losses - (Refer to Section 3.2.2(a) (10), Quarterly Technical Progress Report No. 1) (26) - The bearing friction losses were computed by using a Westinghouse Aerospace Electrical Division developed computer program.

The program utilizes relationships derived for plain cylindrical bearings to calculate a number of bearing performance parameters associated with the bearing power loss. The results have been compared with experimental results for a four pad, pivoted-pad bearing and found to be in good agreement.

Some pertinent assumptions had to be made to provide input for the bearing loss computer program. These assumptions were,

- a. The bearing length to diameter (L/D) ratio was assumed to be 0.75 in all cases.
- b. The bearing radial clearance was assumed to be 0.002 inch in all cases.
- c. The bearing diameter was determined by assuming a design bearing load = 15 psi and for the initial parametric studies where no rotor weight data was available, the bearing diameter was assumed equal to the shaft OD through the stack plus the nominal clearance specified under assumption "b" above.
- d. The fluid (lubricant) properties used were a composite, weighted average of the properties for liquid lithium, potassium, and NaK, since the motor may be required to operate with any of these liquid metals as lubricants.

2.2.2 Status of Parametric Study

The 500°F, 800°F, and 1100°F coolant first cycle frequency studies have been completed. A frequency for the 500°F coolant first cycle speed study was chosen and the 500°F first cycle speed study was completed. Compilation and review of the 800°F coolant first cycle frequency study data was partially completed. Compilation and review of 1100°F first cycle frequency study data and 500°F first cycle speed study data has not begun.

Tables 2.2-2, -3, -4, -5, and -6 are summaries of 5, 30, 60, 100, and 250 horsepower, respectively, design data. Included is design data from the 500°F coolant first cycle frequency study and the 800°F coolant first cycle frequency study (less the detailed temperature calculations). On the basis of the effect of detailed temperature calculations on the results of the 500°F coolant data, the 800°F data shown is considered indicative of the final results of the 800°F first cycle frequency study.

The 500°F, 800°F, and 1100°F coolant first cycle frequency studies were made with voltage = 120 volts line-to-neutral and speed = 12000 rpm. The voltage choice was based on previous parametric studies. The speed choice was based on considerations illustrated by Table 2.2-1, i.e., 12000 rpm is common to all the frequency points considered and to some even number of poles.

Plots of efficiency and weight per horsepower output as a function of frequency were made for use in choosing frequencies for the speed studies. The 500°F coolant curves for 5, 30, 60, 100, and 250 horsepower designs are shown in Figures 2.2-2 through 2.2-11.

The 500°F coolant first cycle frequency study data indicates that, in general, best motor design frequencies for all ratings are between 400 and 800 cps. Preliminary examination of the 800°F coolant first cycle frequency study data (without detailed temperature calculations) reveals the same trend. Based on previous motor parametric studies, it is anticipated that the same trend will extend into the 1100°F coolant frequency study results.

Based on the Figures 2.2-2 through 2.2-11, a frequency of 400 cps was chosen for all ratings for the 500°F coolant first cycle speed study. There are scattered instances (e.g. 500°F, 5 hp designs) where weight and efficiency indicate different "best-frequencies" for the same rating. However, when the entire spread of data is considered, 400 cps appears as the most common "best-frequency" among all the ratings.

Except for the 5 hp ratings at 500°F and 800°F, 200 cps designs appear extremely unfavorable. The necessity to use a two pole design to achieve the specified 12000 rpm speed may be partly responsible for the difficulty. Two-pole designs are inherently relatively inefficient. Consequently, 200 cps, 2-pole designs were eliminated toward the end of the 800°F and 1100°F frequency studies and will be dropped from consideration during the remainder of the study.

Table 2.2-2 Summary of 5 HP Motor Designs, Frequency Study, First Cycle
RPM = 12000; Frequency - Variable; L-N Voltage = 120

Design No.	Coolant Temp. (°F)	Frequency (Hz)	Electrical Weight (lbs)	Actual HP*	Lbs/HP	Efficiency at Rated HP (%)	Electrical Weight plus Penalty (lbs/hp)	Stator OD (in)	Stack Length (in)
10501B	500	200	17.98	4.5	4.00	68.7		4.75	4.13
02B		400	14.28	8.5	1.68	64.3		4.63	3.75
03B		600	13.22	6.5	2.26	64.5		4.38	3.75
04B		800	17.94	7.5	2.39	49.4		4.38	5.50
05B		1000	12.02	6.0	2.00	50.7		4.75	3.25
06B		2000	16.85	5.0	3.37	38.0		5.25	3.63
10501BT	500	200	17.98	3.6	5.00	63.9		4.75	4.13
02BT		400	14.28	7.4	1.93	57.4		4.63	3.75
03BT		600	13.22	6.6	2.00	62.9		4.38	3.75
04BT		800	17.94	7.1	2.53	46.1		4.38	5.50
05BT		1000	12.02	5.5	2.19	43.0		4.75	3.25
06BT		2000	16.85	4.3	3.92	28.7		5.25	3.63
10801B	800	200							
02B		400	15.6	5.0	3.10	58.2		4.75	3.63
03B		600	14.6	4.4	3.32	62.4		4.50	3.75
04B		800	18.0	5.8	3.10	51.2		4.75	4.50
05B		1000	27.6	5.3	5.20	27.4		5.25	5.88
06B		2000	15.3	5.1	3.00	35.6		5.50	3.75

*Established by assuming rated torque = maximum torque/2.25.
T - Suffix following design number indicates temperatures were calculated.

Table 2.2-3 Summary of 30 HP Motor Designs, Frequency Study, First Cycle
RPM = 12000; Frequency - Variable; L-N Voltage = 120

Design No.	Coolant Temp. (°F)	Frequency (Hz)	Electrical Weight (lbs)	Actual HP*	Lbs/HP	Efficiency at Rated HP (%)	Electrical Weight plus Penalty (lbs/hp)	Stator OD (in)	Stack Length (in)
20501B	500 	200	39.8	21	1.90	74.7		6.50	4.75
02B		400	36.1	35	1.03	83.9		6.25	4.75
03B		600	31.0	30	1.03	81.0		5.63	5.00
04B		800	39.6	50	0.79	77.2		6.38	6.13
05B		1000	39.2	40	0.98	72.5		6.25	6.38
06B		2000	74.3	30	2.48	57.8		6.75	10.13
20501BT	500 	200	39.8	15.4	2.58	36.5		6.50	4.75
02BT		400	36.0	36	1.00	83.8		6.25	4.75
03BT		600	29.1	32	0.91	79.1		5.63	4.75
04BT		800	39.5	48	0.82	75.1		6.38	6.13
05BT		1000	39.1	40	0.98	68.7		6.25	6.38
06BT		2000	74.2	30	2.48	50.2		6.75	10.13
20801B	800 	200	66.5	26	2.56	74.9		7.75	5.38
02B		400	37.2	28	1.33	74.3		6.75	4.38
03B		600	30.8	26	1.18	73.2		6.50	4.25
04B		800	36.6	36	1.02	71.6		6.50	5.63
05B		1000	36.1	34	1.06	68.5		6.75	5.38
06B		2000	56.5	35	1.61	53.5		7.88	7.13

* Established by assuming rated torque = maximum torque/2.25.

T - Suffix following design number indicates temperatures were calculated.

Table 2.2-4 Summary of 60 HP Motor Designs, Frequency Study, First Cycle
RPM = 12000; Frequency - Variable; L-N Voltage = 120

Design No.	Coolant Temp. (°F)	Frequency (Hz)	Electrical Weight (lbs)	Actual HP*	Lbs/HP	Efficiency at Rated HP (%)	Electrical Weight plus Penalty (lbs/hp)	Stator OD (in)	Stack Length (in)
30501B	500	200	62.6	40	1.56	75.0		7.25	5.63
02B		400	71.0	75	0.94	88.8		7.25	6.63
03B		600	59.2	60	1.00	85.5		7.13	6.13
04B		800	51.8	49	1.06	84.5		7.38	5.00
05B		1000	46.4	46	1.00	81.8		7.63	4.50
06B		2000	200	104	1.92	68.6		10.5	9.50
30501BT	500	200	57.3	30	1.91	39.8		7.38	4.75
02BT		400	71.0	90	0.79	90.4		7.25	6.63
03BT		600	59.1	70	0.84	86.4		7.13	6.13
04BT		800	51.8	53	0.98	85.0		7.38	5.00
05BT		1000	46.4	47	0.99	80.7		7.63	4.50
06BT		2000	199	100	1.99	65.7		10.5	9.50
30802	800	400	64.5	65	0.99	79.0		7.75	5.88
03		600	55.4	63	0.88	77.5		7.50	6.13
04		800	54.9	62	0.89	79.2		7.75	5.88
05		1000	59.8	66	0.91	74.1		8.00	6.63
06		2000	95.6	80	1.20	53.2		9.50	8.63

*Established by assuming rated torque = maximum torque/2.25.

T - Suffix following design number indicates temperatures were calculated.

Table 2.2-5 Summary of 100 HP Motor Designs, Frequency Study, First Cycle
RPM = 12000; Frequency - Variable; L-N Voltage = 120

Design No.	Coolant Temp. (°F)	Frequency (Hz)	Electrical Weight (lbs)	Actual HP*	Lbs/HP	Efficiency at Rated HP (%)	Electrical Weight plus Penalty (lbs/hp)	Stator OD (in)	Stack Length (in)
40501B	500 	200	126.3	110	1.14	85.1		9.00	8.00
02B		400	75.4	90	0.83	85.3		8.25	5.63
03B		600	67.5	90	0.75	85.7		8.25	5.13
04B		800	102.0	115	0.89	88.2		8.50	7.25
05B		1000	116.0	100	1.16	86.1		9.00	7.25
06B		2000	253.0	115	2.20	78.4		11.5	10.3
40501BT	500 Used for curves	200	126.0	100	1.26	83.2		9.00	8.00
02BT		400	75.4	84	0.90	83.8		8.25	5.63
03BT		600	67.5	84	0.80	85.3		8.25	5.13
04BT		800	99.9	130	0.77	89.7		8.50	8.00
05BT		1000	115.0	124	0.93	87.2		9.25	5.75
06BT		2000	253.0	114	2.22	77.8		11.5	10.3
40801B	800 	200	183.0	105	1.74	81.0		10.5	7.63
02B		400	93.9	103	0.91	79.6		9.5	5.63
03B		600	90.9	130	0.70	83.0		8.9	6.88
04B		800	82.4	131	0.62	80.6		9.1	6.63
05B		1000	74.5	110	0.73	80.2		9.4	6.13
06B		2000	161.0	123	1.21	73.5		11.5	8.75

*Established by assuming rated torque = maximum torque/2.25.

T - Suffix following design number indicates temperatures were calculated.

Table 2.2-6 Summary of 250 HP Motor Designs, Frequency Study, First Cycle
RPM = 12000; Frequency - Variable; L-N Voltage = 120

Design No.	Coolant Temp. (°F)	Frequency (Hz)	Electrical Weight (lbs)	Actual HP*	Lbs/HP	Efficiency at Rated HP (%)	Electrical Weight plus Penalty (lbs/hp)	Stator OD (in)	Stack Length (in)
50501B	500 	200	231.7	240	0.97	86.9	11.25	11.25	8.63
02B		400	166.8	250	0.67	89.6	10.63	10.63	7.50
03B		600	140	200	0.70	88.8	10.50	10.50	6.88
04B		800	173	250	0.69	88.8	10.75	10.75	8.00
05B		1000	151	175	0.86	88.2	10.25	10.25	8.25
06B		2000	412	245	1.68	85.3	15.0	15.0	10.5
50501BT	500 	200	231	235	0.98	85.5	11.25	11.25	8.63
02BT		400	167	260	0.64	89.9	10.63	10.63	7.50
03BT		600	140	180	0.78	89.1	10.50	10.50	6.88
04BT		800	362	208	1.74	93.9	10.75	10.75	17.9
05BT		1000	151	180	0.84	89.2	10.25	10.25	8.25
06BT		2000	412	226	1.83	85.7	15.0	15.0	10.5

* Estimated by assuming rated torque = maximum torque/2.25.

T - Suffix following design number indicates temperatures were calculated.

Figure 2.2-2

Figure 2.2-3

5 HP Motor Frequency Study First Cycle Designs

FREQUENCY # VARIABLE; RPM = 12000
L-N VOLTAGE = 120

Figure 2.2-4

Figure 2.2-5

30 HP Motor Frequency Study First Cycle Designs

Figure 2.2-6

Figure 2.2-7

60 HP Motor Frequency Study First Cycle Designs

Figure 2.2-8

Figure 2.2-9

100 HP Motor Frequency Study First Cycle Designs

Figure 2.2-10

Figure 2.2-11

250 HP Motor Frequency Study First Cycle Designs

Temperature levels were within acceptable, practical limits. The temperature that is usually the most significant in high temperature induction motor design is the winding temperature since the motor hot spot generally occurs at the extreme ends of the hair pin end extensions. The 500°F coolant first cycle frequency study design winding temperatures were all within reasonable, practical limits. In general, these temperatures followed the same trend as the weight/horsepower trends, i.e., were lower for the 400, 600, and 800 cps designs.

2.2.3 Plans for the Next Quarter

The motor parametric study will be completed and the topical report written. Much of the work originally contemplated for the motor parametric study second cycle may be eliminated because of the inherent preciseness presently being required to produce the first cycle data. This may permit much more discrete choice of second cycle designs to study, eliminating the need to investigate all first cycle designs further.

2.3 Frequency Changer Study

ABSTRACT

A parametric study was conducted for three-phase frequency changers to operate primarily with the a-c induction motors evaluated in the AC Motor Parametric Study. The range of parametric data was extended to include low frequency outputs such as might be required for electromagnetic pumps. The purpose of the study was to determine the variation of weight and efficiency as functions of kva, voltage, and frequency ratings.

2.3.1 Design Concepts

All designs use existing components on which adequate design information is available. The cycloconverter approach, without input or output transformers, was used in all designs. Designs are basically those described in Reference 6, having three-phase output.

A. Power Source - The power source is considered to be an infinite bus, i.e., one of negligible impedance, relative to the loaded cycloconverter.

The electric power supply is a four-wire, three-phase, balanced supply having approximately sinusoidal wave form of various specified voltage and frequency levels.

B. Cycloconverter - The basis cycloconverter power circuit consists of a pair of half-wave, thyristor bridges connected in reverse parallel for each output phase. One bridge conducts positive load current while the second bridge conducts negative load current.

The firing of the thyristors are controlled in such a manner as to produce an output voltage as nearly sinusoidal as possible. This is accomplished by appropriately delaying the turn-on of each thyristor until the proper source voltage may be applied to the load. The control scheme is that described in Reference 6.

The cycloconverter power circuit provides direct connections between the source and the load, i.e., transformers are not used. The output voltage is thus a function of the source voltage. The output voltage is approximately 75 percent of the input voltage.

The cycloconverter is current limited to provide a maximum of 1.25 per unit load current by reduction of output voltage to prevent overloading. Normal ratings are, therefore, based on full load.

C. Cycloconverter Power Circuit - The cycloconverter power circuit is shown in Figure 2.3-1. It consists of two basic components, the thyristor and the commutating inductor. Thyristors are arranged in groups of six, each group comprising the switching means for an output phase. In addition, the thyristors are connected in pairs to each input phase through an inductor for bilateral current conduction.

HIGH FREQUENCY
INPUT

Figure 2.3-1

Cycloconverter - Basic Power Circuit

Arrangement for 2 in Parallel (250 KVA)

Arrangement for 4 in Parallel (500 KVA)

Figure 2.3-2
Thyristor Arrangements

The commutating inductors limit the rate of change in current through the thyristors during commutation between input phases. Inductors are located in the circuits to each output phase to provide inter-phase isolation. Without such isolation, rapid changes in voltage would be transmitted to the thyristors with the possibility of causing erratic turn-on.

In the higher cycloconverter ratings of the study, it is necessary to operate thyristors in parallel to obtain the required rating. The method chosen for parallel operation of thyristors involves a modification of Figure 2.3-1 by replacing one inductor and a pair of thyristors by a parallel arrangement such as shown in Figure 2.3-2. In such an arrangement, the inductors act as a load sharing circuit. During the conduction period, the voltage drop in the inductors will be much greater than the difference in thyristor voltage drop and hence, the load division between thyristors becomes a function of the tolerance in the inductors. One need only maintain a reasonable manufacturing tolerance in the commutating inductors and no additional load division means are needed.

2.3.2 Cycloconverter Ratings

Three different parametric variations were included in the study; kva rating, voltage rating, and frequency rating.

A. KVA Rating - Designs were made for ratings of 5, 25, 50, 100, 250, and 500 kva at both 0.5 and 0.7 lagging power factor. All such designs were for a 200-volt, 2000-cps source with 400-cps output frequency.

The two larger ratings, 250-kva and 500-kva, required the use of parallel thyristors.

B. Voltage Rating - Designs were made for voltage ratings of 200, 500, 1000, and 1500 volts. All such designs were rated at 50 kva, 0.7 lagging power factor, 400 cps output with a source frequency of 2000 cps.

Because of the unavailability of sufficiently high voltage thyristors it was necessary to use series connected thyristors in all but the 200-volt designs.

C. Frequency Rating - Designs were made for 50-kva, 0.7 power factor cycloconverters with a 200-volt source for the following frequencies:

<u>Source Frequency</u>	<u>Output Frequencies</u>
800	50, 100, 200, 400
1200	50, 100, 200, 400, 800
2000	50, 100, 200, 400, 800, 1000
3200	50, 100, 200, 400, 800, 1000, 2000

An input to output frequency ratio of 3 : 1 is usually recommended as a minimum. At lower ratios, the ripple current in the motor operating from the cycloconverter will become excessive and reduce the efficiency of the motor. Nevertheless, designs having frequency ratios of less than three have been included for comparison.

2.3.3 Design Calculations

A. Design Procedure

- a. Calculate currents in the power circuit (Figure 2.3-1) based on input voltage and load kva ratings.
- b. Select thyristors based on current and required voltage ratings.
- c. Calculate losses in thyristors and in commutation action.
- d. Design commutating inductor.
- e. Calculate cycloconverter efficiency at full-load output.
- f. Calculate mechanical cooling requirements and unit weight.

B. Current Calculations - For the purpose of design, the load current is considered sinusoidal. Preliminary designs showed that the output voltage would be approximately 75 percent of the input voltage.

The rms value of the fundamental frequency component of load current is,

$$\begin{aligned} I_O &= 10^3 (\text{kva}) / 3V_O \\ &= 10^3 (\text{kva}) / [3 (0.75E_S)] \end{aligned}$$

where V_O is rms load voltage
and E_S is rms source voltage.

The average value of a half cycle of I_O is,

$$\begin{aligned} I_{av} &= (2/\pi) \sqrt{2} I_O \\ &= 0.4 \times 10^3 (\text{kva}) / E_S \end{aligned}$$

The average thyristor current is the average load current divided equally among the thyristors of one phase,

$$\begin{aligned} I_T &= I_{av} / 6 \\ &= 66.7 (\text{kva}) / E_S \end{aligned}$$

The rms current through the commutating inductor is the load current for 1/3 the time, or,

$$\begin{aligned} I_p &= \sqrt{I_o^2/3} \\ &= 0.577 I_o = 257 (\text{kva})/E_s \end{aligned}$$

The maximum inductor current will be higher than the peak load current fundamental component because of the load current ripple. It is assumed that the peak of the load current will be the peak of the fundamental component divided by the half-wave rectifier form factor (0.828).

$$\begin{aligned} I_m &= \sqrt{2} I_o/0.828 \\ &= 762 (\text{kva})/E_s \end{aligned}$$

C. Thyristor Selection - Thyristor ratings were based on the following conditions.

Voltage rating is twice the peak voltage applied in service. In the case of the half-wave cycloconverter, the line-to-line peak voltage will be applied across the thyristor. This is,

$$\sqrt{2} \times \sqrt{3} E_s = 2.45 E_s$$

The thyristor rating is then approximately five times the source voltage line-to-neutral rms voltage.

The thyristor current rating is based on the average thyristor current, I_T . It is assumed that the heating effect is approximately the same as that in half-wave rectification with a 60 degree conduction angle. The standard rating curve, published by the thyristor manufacturer, was used to determine the temperature rise and power loss functions of average current.

The maximum allowable thyristor operating temperature is based on a junction temperature of 125°C in most cases. In this study, a de-rating in current was applied such that full-load current would result in a maximum temperature of 110°C. This was done by designing the cooling mechanism to maintain the maximum thyristor stud temperature 15°C less than that allowed by the manufacturer's published curve (based on 125°C junction temperature).

When the thyristor is first turned on, the current is concentrated in one spot and spreads over the total junction as a function of time. If the current builds up too fast in the junction, a hot spot can develop and destroy the junction. The rate of rise of thyristor current is thus limited to a value of 30 amperes per microsecond. This restriction limits the rating of the thyristors used in these designs. Thus, the 250-kva unit required the use of two thyristors in parallel per switch, and the 500-kva unit requires four.

The thyristors selected for each design are shown in Tables 2.3-1 to 2.3-7.

D. Thyristor Switching Losses - In addition to the losses caused by current conduction of the thyristor, there are switching losses and gate losses. Gate losses are assumed to be the rated values published in the manufacturer's data sheets.

Accurate calculation of switching losses of a thyristor in a cycloconverter circuit is an involved, step-by-step procedure. It involves the calculation of incremental power as the current spreads within the junction.

Experience with cycloconverters having commutating inductors has shown that switching losses are a small part of the total thyristor losses. The bulk of switching loss occurs during the turn-on period. Since there were a good many designs involved in the study, and because the switching losses are not high, some simplifying assumptions were made in the calculations.

Turn-off losses are neglected. Judging from published data, it was assumed that the voltage, v , across the thyristor falls at a constant rate during turn-on. Each time a thyristor turns on, another is commutated off; thus, the current is limited by two commutating inductors, L , in series. Turn-on takes place fast enough so that source voltage, V_m , may be assumed to be constant during the turn-on period, T_R , thus,

$$2L \frac{di}{dt} = V_m - v = \frac{V_m}{T_R} t \quad (\text{volts})$$

$$\text{or,} \quad di = \frac{V_m}{2LT_R} t \, dt$$

$$\text{and} \quad i = \frac{V_m}{2LT_R} \cdot \frac{t^2}{2} \quad (\text{amperes})$$

$$\text{also,} \quad v = V_m (1 - t/T_R)$$

$$\text{so,} \quad vi = \frac{V_m^2}{4L} (t/T_R - t^3/T_R^2) \quad (\text{watts})$$

$$\text{and} \quad W = \int_0^{T_R} (vi) dt \quad (\text{joules})$$

Table 2.3-1 Cycloconverter Design Summary, 200 Volt, 2000/400 CPS,
Power Factor 0.5

Rating (kva)	5	25	50	100	250	500
Thyristor	GE C11E	(W) 219Z	(W) 219Z	(W) 254	(W) 218	(W) 218
No. in Series	2	1	1	1	1	1
No. in Parallel	1	1	1	1	2	4
Max. Temperature (Case) (°C)	90	100	90	82	95	95
Thyristor Losses (Watts)	3.31	19.54	43.1	71.8	75	75
Conduction Loss	2.7	16	39	65	68	68
Switching Loss	0.11	0.54	1.1	3.8	4	4
Gate Loss	0.5	3	3	3	3	3
Inductance, L_C (μ -hen.)	173	34.6	17.3	8.65	8.16	8.16
Coil Temperature (°C)	140	140	140	140	140	140
Loss (Watts)	2.13	7.13	12.0	20.2	27	27
Weight (pounds)	0.0761	0.255	0.428	0.719	0.962	0.962
Dimension "a" (inches)	0.307	0.458	0.545	0.648	0.714	0.714
Control Loss (watts)	48.5	69	69	69	93	136
Control Weight (pounds)	4.4	6.4	6.4	6.4	8.9	12.1
*Total Losses (Watts)	187	488	951	1541	3283	6516
Input Current RMS (amp)	13.4	67.1	134	268	671	1340
Output Current RMS (amp)	11.1	55.6	111	222	555	1110
Inductor Current RMS (amp)	6.42	32.1	64.2	128	160.4	160.4
Thyristor Current RMS (amp)	4.54	22.7	45.4	90.8	113.5	113.5
Thyristor Current Average	1.67	8.3	11.7	33.4	47.5	41.5
Rating (kva)	5	25	50	100	250	500
V_{FW} (Thyristor)	2.8	1.6	2.0	1.27	1.35	1.35
V_{CO} (Commutating)	10	10	10	10	12	12
V_C (Inductor)	7.09	7.09	7.09	7.09	8.3	8.3
$0.828 (E_S - V_{CO} - V_C)$	151.5	151.5	151.5	151.5	14.88	14.88
V_O (Load)	148.7	149.9	149.5	150.2	147.4	147.4
Output (kva)	4.95	25	49.8	100	245.5	491
Efficiency (%)	92.5	96.1	96.2	96.9	97.3	97.35
Weight (pounds)	17.8	31.3	33.1	36.2	68.7	119.1

*Total Loss = Control Loss + 9(No. Parallel) [2(No. Series) (Thy. Loss) + Ind. Loss]

Table 2.3-2 Cycloconverter Design Summary, 200 Volt, 2000/400 CPS,
Power Factor 0.7

Rating (kva)	5	25	50	100	250	500
Thyristor	GE C11E	(W) 219Z	(W) 219Z	(W) 254	(W) 218	(W) 218
No. in Series	2	1	1	1	1	1
No. in Parallel	1	1	1	1	2	4
Max. Temperature (°C) (Case)	90	100	90	82	95	95
Thyristor Losses (Watts)	3.31	19.6	43.2	72	75.1	75.1
Conduction Loss	2.7	16	39	65	68	68
Switching Loss	0.11	0.6	1.2	4	4.1	4.1
Gate Loss	0.5	3	3	3	3	3
Inductance, L_c (μ -hen.)	167	33.4	16.7	8.35	8.16	8.16
Coil Temperature (°C)	140	140	140	140	149	140
Loss (Watts)	2.08	6.94	11.7	19.6	27	27
Weight (pounds)	0.0740	0.248	0.416	0.700	0.962	0.962
Dimension "a" (inches)	0.303	0.454	540	0.642	0.714	0.714
Control Loss (Watts)	48.5	69	69	69	93	136
Control Weight (Pounds)	4.4	6.4	6.4	6.4	8.9	12.1
*Total Losses (Watts)	187	488	951	1541	3283	6516
Input Current RMS (amp)	13.4	67.1	134	268	671	1340
Output Current RMS (amp)	11.1	55.6	111	222	555	1110
Inductor Current RMS (amp)	6.42	32.1	64.2	128.5	160.4	160.4
Thyristor Current RMS (amp)	4.54	22.7	45.4	90.8	113.5	113.5
Thyristor Current Average	1.67	8.3	16.7	33.4	47.5	41.5
Rating (kva)	5	25	50	100	250	500
V_{FW} (Thyristor)	2.8	1.6	2.0	1.27	1.35	1.35
V_{co} (Commutating)	10	10	10	10	12.5	12.5
V_c (Inductor)	8.05	8.05	8.05	8.05	9.76	9.76
$0.828 (E_s - V_{co} - V_c)$	150.7	150.7	150.7	150.7	147.1	147.1
V_o (Load)	147.9	149.1	148.7	149.4	145.7	145.7
Output (kva)	4.93	24.85	49.5	99.5	243	485
Efficiency (%)	94.6	97.2	97.25	97.8	98.07	98.08
Weight (pounds)	17.8	31.2	33.0	36.0	68.7	117.8
*Total Loss = Control Loss + 9(No. Parallel) [2(No. Series) (Thy. Loss) + Ind. Loss]						

Table 2.3-3 Cycloconverter Design Summary, 50-KVA, 200/400 CPS, Power Factor 0.7

Source Voltage	200	500	1000	1500
Thyristor	(W) 219Z	(W) 219ZD	(W) 219ZD	(W) 219ZH
No. in Series	1	2	4	5
No. in Parallel	1	1	1	1
Max. Temperature (°C)	90	105	108	109
Thyristor Losses (watts)	43.2	16.6	9.3	7.7
Conduction Loss	39	13	6	4.5
Switching Loss	1.2	0.6	0.3	0.2
Gate Loss	3	3	3	3
Inductance L_C (μ -hen)	16.7	104.3	417	939
Coil Temperature (LC)	140	140	140	140
Loss (watts)	11.7	11.7	11.7	11.7
Weight (pounds)	0.416	0.41	0.416	0.416
Dimension "a" (inches)	0.540	0.5	0.540	0.540
Control Loss (watts)	69	94	136	152
Control Weight (pounds)	6.4	8.9	13.1	14.7
*Total Losses (watts)	951	796	912	950
Input Current RMS (amp)	134	53.7	26.8	17.9
Output Current RMS (amp)	111	44.4	22.2	14.8
Inductor Current RMS (amp)	64.2	25.7	12.83	8.55
Thyristor Current RMS (amp)	-	-	∞	-
Thyristor Current Average	16.7	6.7	3.3	2.2
Source Voltage	200	500	1000	1500
V_{FW} (Thyristor)	2.0	4.0	8.0	10.0
V_{CO} (Commutating)	10	25	50	75
V_C (Inductor)	8.05	20.2	40.3	60.4
$0.828 (E_S - V_{CO} - V_C)$	150.7	377	753	1130
V_O (Load)	148.7	373	745	1120
Output (kva)	49.5	49.8	49.7	49.8
Efficiency (%)	97.26	97.72	97.38	97.28
Weight (pounds)	33.0	45.8	71.9	83.2
*Total Loss = Control Loss + 9(No. Parallel) [2(No. Series) (Thy. Loss) + Ind. Loss]				

Table 2.3-4 Cycloconverter Design Summary, 50-KVA, 200 Volt, 800 CPS Input, Power Factor 0.7

Output Frequency (cps)	50	100	200	400
Thyristor	(W) 219Z	(W) 219Z	(W) 219Z	(W) 219Z
No. in Series	1	1	1	1
No. in Parallel	1	1	1	1
Max. Temperature (°C)	90	90	90	90
Thyristor Losses (watts)	42.2	42.2	42.2	42.1
Conduction Loss	39	39	39	39
Switching Loss	0.22	0.2	0.17	0.1
Gate Loss	3	3	3	3
Inductance, L_C (μ -hen.)	38.7	40	42.9	50.2
Coil Temperature (°C)	140	140	140	140
Loss (watts)	16.9	17.9	20.3	22.7
Weight (pounds)	0.822	0.838	0.872	0.951
Dimension "a" (inches)	0.678	0.682	0.691	0.712
Control Loss (watts)	79	79	79	79
Control Weight (pounds)	13.9	12.2	9.8	7.4
*Total Losses (watts)	991	1000	1029	1041
Input Current RMS (amp)	134	134	134	134
Output Current RMS (amp)	111	111	111	111
Inductor Current RMS (amp)	64.2	64.2	64.2	64.2
Thyristor Current RMS (amp)	45.4	45.4	45.4	45.4
Output Frequency (cps)	50	100	200	400
V_{FW} (Thyristor)	2.0	2.0	2.0	2.0
V_{CO} (Commutating)	10	10	10	10
V_C (Inductor)	2.5	5	10	19
$0.828 (E_S - V_{CO} - V_C)$	155.3	153.2	149	141.8
V_O (Load)	153.3	151.2	147	139.8
Output (kva)	51.1	50.4	49	46.6
Efficiency (%)	97.23	97.17	97.0	96.81
Weight (pounds)	45.2	43.7	41.6	40.1
*Total Loss = Control Loss + 9(No. Parallel) [2(No. Series) (Thy. Loss) + Ind. Loss]				

Table 2.3-5 Cycloconverter Design Summary, 50-KVA, 200 Volt, 1200 CPS Input,
Power Factor 0.7

Output Frequency (cps)	50	100	200	400	800
Thyristor	(W) 219Z	(W) 219Z	(W) 219Z	(W) 219Z	(W) 219Z
No. in Series	1	1	1	1	1
No. in Parallel	1	1	1	1	1
Max. Temperature (°C)	90	90	90	90	90
Thyristor Losses (watts)	42.5	42.5	42.4	42.3	42.16
Conduction Loss	39	39	39	39	39
Switching Loss	0.5	0.5	0.4	0.3	0.16
Gate Loss	3	3	3	3	3
Inductance, L_C (μ -hen.)	25.5	25.9	27.3	30.1	37.7
Coil Temperature (°C)	140	140	140	140	140
Loss (watts)	12.3	13.0	14.2	18.2	27.3
Weight (pounds)	0.601	0.608	0.621	0.647	0.718
Dimension "a" (inches)	0.611	0.613	0.617	0.626	0.648
Control Loss (watts)	74	74	74	74	74
Control Weight (pounds)	13.4	11.7	9.4	7.0	6.9
*Total Losses (watts)	949	956	965	999	1056
Input Current RMS (amp)	134	134	134	134	134
Output Current RMS (amp)	111	111	111	111	111
Inductor Current RMS (amp)	64.2	64.2	64.2	64.2	64.2
Thyristor Current RMS (amp)	45.4	45.4	45.4	45.4	45.4
Output Frequency (cps)	50	100	200	400	800
V_{FW} (Thyristor)	2.0	2.0	2.0	2.0	2.0
V_{CO} (Commutating)	10	10	10	10	10
V_C (Inductor)	1.6	3.3	6.7	13.3	25.3
$0.828 (E_S - V_{CO} - V_C)$	0.156	154.5	152	146	136
V_O (Load)	154	152.5	150	144	134
Output (kva)	51.3	50.8	50	48	44.7
Efficiency (%)	97.36	97.31	97.24	97.02	96.63
Weight (pounds)	41.8	40.2	38.0	36.1	36.7
*Total Loss = Control Loss + 9(No. Parallel) [2(No. Series) (Thy. Loss) + Ind. Loss]					

Table 2.3-6 Cycloconverter Design Summary, 200 Volt, 2000 CPS Input,
Power Factor 0.7

Output Frequency (cps)	50	100	200	400	800	1000
Thyristor	(W) 219Z	(W) 219Z	(W) 219Z	(W) 219Z	(W) 219Z	(W) 219Z
No. in Series	1	1	1	1	1	1
No. in Parallel	1	1	1	1	1	1
Max. Temperature (°C)	90	90	90	90	90	90
Thyristor Losses (watts)	43.5	43.4	43.3	43.1	42.9	42.7
Conduction Loss	39	39	39	39	39	39
Switching Loss	1.5	1.4	1.3	1.1	0.9	0.7
Gate Loss	3	3	3	3	3	3
Inductance, L_C (μ -hen.)	15.2	15.4	15.8	16.7	18.1	19
Coil Temperature (°C)	140	140	140	140	140	140
Loss (watts)	8.4	8.6	9.6	11.7	17.0	20.3
Weight (pounds)	0.407	0.409	0.412	0.416	0.426	0.433
Dimension "a" (inches)	0.536	0.537	0.538	0.540	0.544	0.547
Control Loss (watts)	69	69	69	69	69	69
Control Weight (pounds)	12.8	11.2	8.8	6.4	6.3	6.3
*Total Losses (watts)	922	929	935	951	1005	1021
Input Current RMS (amp)	134	134	134	134	134	134
Output Current RMS (amp)	111	111	111	111	111	111
Inductor Current RMS (amp)	64.2	64.2	64.2	64.2	64.2	64.2
Thyristor Current RMS (amp)	45.4	45.4	45.4	45.4	45.4	45.4
Output Frequency (cps)	50	100	200	400	800	1000
V_{FW} (Thyristor)	2.0	2.0	2.0	2.0	2.0	2.0
V_{CO} (Commutating)	10	10	10	10	10	10
V_C (Inductor)	1.0	2.0	4.0	8.1	15.8	19.5
$0.828(E_s - V_{ca} - V_C)$	156.5	155.5	154	150.5	144	141
V_O (Load)	154.5	153.5	152	148.5	142	139
Output (kva)	51.5	51.2	50.7	49.5	47.4	46.4
Efficiency (%)	97.46	97.41	97.36	97.25	96.97	96.85
Weight (pounds)	39.3	37.8	35.4	33.0	33.0	33.1
*Total Loss = Control Loss + 9(No. Parallel) [2(No. Series) (Thy. Loss) + Ind. Loss]						

Table 2.3-7 Cycloconverter Design Summary, 50-KVA, 200 Volt, 3200 CPS Input,
Power Factor 0.7

Output Frequency (cps)	50	100	200	400	800	1000	2000
Thyristor	(W) 219Z	(W) 219Z	(W) 219Z	(W) 219Z	(W) 219Z	(W) 219Z	(W) 219Z
No. in Series	1	1	1	1	1	1	1
No. in Parallel	1	1	1	1	1	1	1
Max. Temperature (°C)	90	90	90	90	90	90	90
Thyristor Losses (watts)	45.8	45.7	45.5	45.2	44.7	44.4	43.3
Conduction Loss	39	39	39	39	39	39	39
Switching Loss	3.8	3.7	3.5	3.2	2.7	2.4	1.3
Gate Loss	3	3	3	3	3	3	3
Inductance, L_c (μ -hen.)	9.45	9.53	9.68	10.0	10.7	11.1	13.7
Coil Temperature (°C)	140	140	140	140	140	140	140
Loss (watts)	5.9	6.1	6.6	8.0	11.2	13.0	24.2
Weight (pounds)	0.285	0.286	0.286	0.284	0.280	0.278	0.279
Dimension "a" (inches)	0.476	0.476	0.476	0.475	0.473	0.472	0.473
Control Loss (watts)	62	62	62	62	62	62	62
Control Weight (pounds)	12.6	11.0	8.6	6.2	6.1	6.1	6.0
*Total Losses (watts)	940	940	941	948	967	1014	1059
Input Current RMS (amp)	134	134	134	134	134	134	134
Output Current RMS (amp)	111	111	111	111	111	111	111
Inductor Current RMS (amp)	64.3	64.3	64.3	64.3	64.3	64.3	64.3
Thyristor Current RMS (amp)	45.4	45.4	45.4	45.4	45.4	45.4	45.4
Output Frequency (cps)	50	100	200	400	800	1000	2000
V _{FW} (Thyristor)	2.0	2.0	2.0	2.0	2.0	2.0	2.0
V _{CO} (Commutating)	10	10	10	10	10	10	10
V _C (Inductor)	0.6	1.2	2.5	5.0	10	12.5	23.9
0.828 (E _s -V _{CO} -V _C)	0.157	156.5	155	153.2	149	147	145
V _O (Load)	155	154.5	153	151.2	147	145	143
Output (kva)	51.7	51.5	51	50.4	49	48.4	47.7
Efficiency (%)	97.4	97.39	97.36	97.31	97.18	97.0	96.83
Weight (pounds)	37.9	36.3	33.9	31.5	31.4	31.3	31.3
*Total Loss = Control Loss + 9(No. Parallel) [2(No. Series) (Thy. Loss) + Ind. Loss]							

$$\begin{aligned}
&= \frac{V_m^2}{4L} \left(\frac{T_R^2}{3} - \frac{T_R^2}{4} \right) \\
&= \frac{V_m^2 T_R^2}{48L} \text{ joules per turn-on.}
\end{aligned}$$

The number of commutations per output cycle per thyristor bank is determined as follows.

When the cycloconverter produces full output voltage, switching occurs at two discrete rates. If the output voltage is rising and the load current is positive, the number of switching points of the positive thyristor bank during the time period is,

$$\begin{aligned}
n_a &= \frac{p}{w_o} (w_s + w_o) (\pi/2 - \theta) / 2\pi \\
&= p(k + 1) (\pi/2 - \theta) / 2\pi.
\end{aligned}$$

When the output voltage is falling and the current is positive, the number of switching points in the positive bank is,

$$n_b = p(k - 1) (\pi/2 + \theta) / 2\pi$$

where

$$\begin{aligned}
p &= \text{number of phases} \\
w_s &= \text{source frequency} \times 2\pi \\
w_o &= \text{output frequency} \times 2\pi \\
k &= w_s / w_o \\
\theta &= \text{load current lag angle.}
\end{aligned}$$

The switching loss per output cycle may be described as the average loss per switching point times the number of switching points per cycle. The commutating voltage at any switching point is

$$V_m = E_{cm} \sin w_o t,$$

where the output-voltage fundamental component is

$$V_{om} \cos w_o t.$$

We have assumed that turn-on occurs fast enough that V_m may be considered constant during the turn-on period. The average switching loss due to turn-on may then be expressed as follows.

The average energy loss per switching point is,

$$W_{av} = \frac{T_R^2}{48L} (V_m^2 \text{ average})$$

The energy loss per output cycle is,

$$\begin{aligned}
 W_f &= \frac{2E_{cm}^2 T_R^2}{48L} \left[\frac{n_a w_o}{\pi/2 - \theta} \int_0^{(\pi/2 - \theta)/w_o} \frac{1}{\sin^2 w_o t} dt \right. \\
 &\quad \left. + \frac{n_b w_o}{\pi/2 + \theta} \int_0^{(\pi/2 + \theta)/w_o} \frac{1}{\sin^2 w_o t} dt \right] \\
 &= \frac{E_{cm}^2 T_R^2}{24L} \left[\frac{n_a w_o}{\pi/2 - \theta} \left(\frac{\pi/2 - \theta}{2w_o} - \frac{\sin 2\theta}{4w_o} \right) \right. \\
 &\quad \left. + \frac{n_b w_o}{\pi/2 + \theta} \left(\frac{\pi/2 + \theta}{2w_o} + \frac{\sin 2\theta}{4w_o} \right) \right]
 \end{aligned}$$

Substituting the values for n_a and n_b and reducing yields (for three-phase),

$$W_f = \frac{E_{cm}^2 T_R^2}{32\pi L} [k\pi - 2\theta - \sin 2\theta] \text{ (joules per cycle)}$$

Power dissipation due to turn-on is,

$$\begin{aligned}
 P_{to} &= W_f(f_o) \\
 &= \frac{3E_s^2 T_R^2}{16\pi L} (k\pi - 2\theta - \sin 2\theta) f_o \text{ (watts)}
 \end{aligned}$$

where E_s is rms line-to-neutral source voltage and f_o is output frequency in cps.

The power dissipated in each thyristor due to turn-on is,

$$P_{sw} = P_{to}/6 = \frac{E_s^2 T_R^2}{32\pi L} (k\pi - 2\theta - \sin 2\theta) f_o \text{ (watts)}$$

In the study, the turn-on time, T_R , was assumed to be 3 micro-seconds for ratings below 100 kva and 4 microseconds for 100 kva and greater.

E. Commutation Circuit Design - Each time a thyristor is turned on, commutation takes place between two source phases connecting the input circuits to two thyristors together. Line-to-line source voltage forces current through the two thyristors and the inductors connected in series with each. Only the inductors limit the commutation current until the current in the previously conducting thyristor falls to zero.

During the commutation period, the cycloconverter output voltage is the instantaneous average of the two phase voltages being commutated(7). This is a decrease from the normal output voltage.

The functions of the commutating inductance are (1) to limit rate of rise of commutating current through the thyristors and (2) to provide some isolation between output phases against rapid voltage changes. The value of inductance used is a compromise.

In the designs of this study, the inductance is chosen so as to produce a reduction in output voltage equal to 5 percent of the source voltage if the rate of rise of current is less than 30 amperes per microsecond.

First consider the current limit; the commutating current is (7),

$$i_c = -I_{cm} \cos wt$$

$$di_c/dt = w_s I_{cm} \sin wt$$

$$di_c/dt \text{ (maximum)} = E_{cm}/2L$$

$$= \sqrt{3}/2 E_s/L \text{ (amperes per second).}$$

This equation defines the minimum value of inductance L.

Now consider the commutation voltage loss. The loss in output volt-seconds during a single commutation period is (7),

$$\begin{aligned} vt &= \int_{\alpha}^{\alpha + u} (e_2 - e_u) dt \\ &= \frac{1}{2} \int_{\alpha}^{\alpha + u} e_c dt \end{aligned}$$

where e_2 is the phase voltage being switched on,
 e_u is the average of the two voltages being commutated,
 α is the firing angle,
 u is the commutating angle, and
 e_c is the commutating voltage.

For three-phase, half wave, $p = 3$ and,

$$E_c = E_{cm} \sin w_s t$$

Thus,

$$\begin{aligned}
 v_t &= \frac{E_{cm}}{2} \int_{\alpha}^{\alpha + u} \sin w_s t \, dt \\
 &= -\frac{E_{cm}}{2} \left[\cos w_s t \right]_{\alpha}^{\alpha + u} \\
 &= \frac{E_{cm}}{2w_s} \left[-\cos (\alpha + u) + \cos \alpha \right] \\
 &= \frac{E_{cm}}{2w_s} \left[\frac{I_c X_c}{E_s \sqrt{2} \sin \pi/p} \right], \quad (\text{Ref. 7}) \\
 &= \frac{I_c X_c}{w_s} = I_c L \quad (\text{volt-seconds per commutation})
 \end{aligned}$$

The number of commutations per output half cycle is,

$$n_a + n_b = 3(k/2 - \theta/\pi)$$

The value of I_c varies approximately as the output load current. Therefore, the loss in output voltage may be expressed as,

$$V_{CO} = 3(k/2 - \theta/\pi) \frac{w_o}{\pi} L I_o$$

from which L may be computed.

In addition to the commutation voltage drop, a further voltage drop is due to the change in current occurring after commutation is complete. Since the current is driven by the source voltage, it is reasonable to assume that the voltage drop is responsive to the source voltage driving through the total impedance (commutating inductance plus the load).

Assume a load impedance of,

$$\begin{aligned}
 Z &= \sqrt{R^2 + (w_o L_o)^2} \\
 &= \sqrt{R^2 + (w_s L_o / k)^2}
 \end{aligned}$$

$$\text{or } w_s L_o = k \sqrt{Z^2 - R^2} = kZ \sqrt{1 - (\text{PF})}$$

$$\text{where } Z = 3V_o^2 \times 10^{-3} / (\text{kva}),$$

$$R = Z (\text{PF})$$

and PF is the load power factor. The voltage drop in the commutation inductance is,

$$\begin{aligned} V_C &= \frac{j\omega_S L}{R + j\omega_S (L + L_O)} E_S \\ &= \frac{\omega_S L (\omega_S L + \omega_S L_O + jR) E_S}{R^2 + (\omega_S L + \omega_S L_O)^2} \end{aligned}$$

In the calculations, the out-of-phase term was neglected since its contribution was small.

F. Commutating Inductor - Investigation of iron-core and air-core inductors showed the iron-core inductor to be more efficient and lighter weight for this application.

A configuration was assumed as shown in Figure 2.3-3. Design equations for inductor weight and loss were developed as follows.

$I_m = 762 \text{ (kva)}/E_S = \text{peak inductor current}$

$V_C = 4.17a^3 = \text{coil volume}$

$V_i = 6a^3 = \text{core volume}$

$\phi = PNI = Ba^2 = \text{flux}$

$L = N^2 P = \text{inductance}$

$P = \mu a^2/g = \text{permeance of air gap.}$

where $N = \text{number of turns in coil}$

$I = \text{coil current}$

$B = \text{flux density}$

$\mu = 3.19 \times 10^{-8} \text{ webers per square inch.}$

$$a^2 = \frac{LI_m}{NB_m} = \frac{LI_m}{B_m} \left(\frac{P}{L} \right)^{1/2} = \frac{I_m}{B_m} (LP)^{1/2}$$

$$= \frac{I_m}{B_m} (\mu a^2/g)^{1/2} L^{1/2}$$

$$\begin{aligned} \text{Thus, } a &= \left(\frac{I_m}{B_m} \right)^{2/3} (\mu L)^{1/3} (a/g)^{1/3} \\ &= G(a/g)^{1/3} \text{ where } G = \left(\frac{I_m}{B_m} \right)^{2/3} (\mu L)^{1/3} \end{aligned}$$

The inductor weight equals volume times density and is,

$$\begin{aligned} w_L &= 4.17a^3(0.23) + 6a^3(0.28) \\ &= 2.64a^3 = 2.64 G^3(a/g) \end{aligned}$$

Figure 2.3-3
Iron-Core Inductor

The coil current density is,

$$J = 4 NI / (3a^2) \quad (\text{amperes per square inch})$$

The power loss in the coil is,

$$p_c = 4.17 J^2 \rho a^3 / s \quad (\text{watts})$$

where ρ = resistivity, ohm-inch
 s = ratio of conductor area to coil cross-section area.

$$\begin{aligned} \text{or } p_c &= \frac{4.17 \rho a^3}{s} \cdot \frac{16 N^2 I_p^2}{9 a^4} = 7.42 \frac{\rho}{s} \cdot \frac{N^2 I_p^2}{a} \\ &= 7.42 \frac{\rho}{s} \cdot \frac{I_p^2}{a} \cdot \frac{L}{P} \\ &= 7.42 \frac{\rho I_p^2}{s} \left(\frac{B_m}{I_m} \right)^{4/3} \cdot \frac{L^{1/3}}{(\mu a / g)^{5/3}} \\ &= H / (a/g)^{5/3} \end{aligned}$$

$$\text{where } H = 7.42 \frac{\rho I_p^2 L^{1/3}}{s \mu^{5/3}} \left(\frac{B_m}{I_m} \right)^{4/3}.$$

Power loss in the iron core is,

$$\begin{aligned} p_i &= p_p (w_i) \\ &= p_p (0.28) 6a^3 = 1.68 p_p a^3 \\ &= 1.68 p_p G^3 (a/g) \end{aligned}$$

where p_p = watts per pound core loss

$$= 3.03 \times 10^{-3} f_o^{1.313}$$

and w_i = pounds iron core.

Assume a system weight penalty of F pounds per watt loss. Additional weight of the system due to the inductor loss is,

$$w_p = F(p_i + p_c)$$

$$= F \left[1.68 p_p G^3 (a/g) + H/(a/g)^{5/3} \right]$$

The minimum system-weight penalty due to inductors is obtained where,

$$\frac{d(w_p + w_L)}{d(a/g)} = 0$$

$$\text{Now,} \quad dw_p/d(a/g) = 1.68 p_p F G^3 - 5/3 F H (a/g)^{-8/3}$$

$$\text{and,} \quad dw_L/d(a/g) = 2.64 G^3$$

$$\text{so,} \quad a/g = \left[\frac{(5/3) F H}{(1.68 p_p F + 2.64) G^3} \right]^{3/8}$$

For calculation of the inductor weight and losses, the following values were used.

$$\begin{aligned} B_m &= 10^{-3} \text{ webers per square inch} \\ F &= 30 \text{ watts per pound} \\ \rho &= 10^{-6} \text{ ohm-in (copper, } 140^\circ\text{C)} \end{aligned}$$

G. Cycloconverter Control Weight and Losses - The control for the cycloconverter is basically the same as described in Reference 6 (pages 15/16 and 81/82). The three-phase, control reference generator is described in Reference 8 (pages IV-1 to IV-7). In this study, the weight and loss estimates were based on the use of integrated circuits where possible, rather than the discrete components shown in Reference 6.

Some transformers used in the control circuit operate at source frequency, while others operate at output frequency. The calculated weights of control transformers are plotted as functions of the appropriate frequency variation in Figure 2.3-4 and Figure 2.3-5. Weight of the cycloconverter control was computed on the basis of a fixed weight plus transformer weight which varies with rating, Figure 2.3-6.

$$\text{Weight} = 1.3 + w_4 + w_5 \quad (\text{pounds})$$

where w_4 is shown in Figure 2.3-4 and w_5 is shown in Figure 2.3-5.

Losses in the transformers were computed as functions of frequency with a design limit of 10 watts per pound of core loss.

Figure 2.3-4

Weight versus Frequency

Figure 2. 3-5

Weight versus Frequency as a Function of KVA

Figure 2. 3-6

Transformer Weight

$$\text{Losses} = 10w_5 + P_F + 16.8 \quad (\text{watts})$$

where $P_F = (3 \times 10^{-3} f^{1.313}) w_4$ when $f < 400$ cps

and $P_F = 10w_4$ when $f \geq 400$ cps.

The following assumptions were made.

- a. Component weight (other than transformers) = 0.001 pound.
- b. Weight of each interconnection = 0.001 pound.
- c. A safety factor = 10% of total a. plus b. was added.
- d. Core losses equal 10 watts per pound for all transformers used at 400 cps or greater.
- e. Core losses equal $3(10)^{-3} f^{1.313}$ watts per pound for all transformers used for frequencies lower than 400 cps.
- f. Weight of copper for 400 cps is,

$$W_{cu} = \frac{KWR \times \Delta T (235 + T)^{0.25} (VA)^{0.49}}{(f B_n)^{0.63}}$$

- g. Weight of core for 400 cps is,

$$W_{FE} = \frac{KWI \times JI (235 + T)^{0.25} (VA)^{0.8}}{BA(f)^{0.94} \Delta T^{0.38}}$$

- h. Weight of transformers varies with frequency as shown in Figure 2.3-6.

where

$$KWR \equiv 0.42$$

$$KWI \equiv 44$$

$$\Delta T \equiv \text{operating temperature}$$

$$f \equiv \text{frequency of operation}$$

$$B_n \equiv \text{maximum operating flux density}$$

$$B_A = B_n + (B_n - 10)^3 (0.007)$$

$$JI \equiv \text{actual watts per pound}$$

$$VA \equiv \text{volt-ampere requirements of transformer.}$$

H. Efficiency and Output Rating - Having calculated the losses and the voltage drops, the output rating (which was initially assumed) was then computed. These ratings correlated reasonably well with the assumed ratings. The ratings are listed in the summary tables (2.3-1 to 2.3-7).

The load voltage was computed as,

$$V_O = 0.828 (E_S - V_{CO} - V_C) - V_{FW},$$

where E_S = source rms line-to-neutral voltage,
 V_{CO} = the commutating voltage loss (rms),
 V_C = inductor voltage drop (rms)
 V_{FW} = thyristor voltage drop (rms)

and (0.828) is the half-wave rectification form factor.

The output kva was computed using the computed output voltage and the assumed load current I_O .

$$kva = 3V_O I_O \times 10^{-3}$$

$$\text{Efficiency} = 100 \left[1.00 - \frac{\text{losses}}{kva(PF)} \right] \text{ (percent)}.$$

2.3.4 Mechanical Design

The evaluation of frequency changer cooling was based on the following assumptions.

- a. Monoisopropyl Biphenyl (MIPB) is the coolant
- b. Cooling is by conduction, employing the cold plate method.
- c. Coolant weight penalty considerations included in the frequency changer total weight were limited to the entrapped coolant weight, weight of coolant ducts, cold plates and the associated component insulation.

In all systems, the thyristors are adhesive-bonded to beryllium oxide blocks which are in turn bonded to the cold plate. The beryllium oxide is used to electrically insulate the thyristor from the cold plate and provide good thermal conduction for heat rejection. The inductors are mounted directly to the cold plate. Adhesive bonding is used to reduce the thermal resistance across the interface of the two surfaces. The thyristor gate-control components are cooled by the same method.

The coolant ducts are an integral part of the cold plate which provides direct contact between the coolant and the plate to which the components are mounted. The outside package, cold plates, and coolant ducts are of aluminium alloy. A conceptual sketch of this cooling method is presented in Figure 2.3-7.

Figure 2.3-7
Frequency Changer System Cooling

Operating temperature of each system was determined by the thyristor characteristics when the design of the system was established. The coolant average temperature is specified as 10°F less than the difference between the operating temperature and the temperature drop from the thyristor to the coolant. The temperature drop from the thyristor to the coolant was calculated as the sum of the temperature drops in the thyristor base, the insulation, the cold plate and the adhesive. These calculations were made using the physical properties of the materials involved at temperatures of each system.

The weight of each system includes total component weight, cold plate and entrapped coolant weight, insulation and package weight. Weights of the cold plate, entrapped coolant and insulation were calculated using weights per unit area of cold-plate surface determined from sample calculations of individual cold plates. The package weights were based on a volume-to-weight relationship established from several sample package weight calculations. The cold plate, entrapped coolant, and package weights for the thyristor control components are generally a small percent of the total and were assumed constant for all systems. This approach was taken because design details of these "packaging" components were not calculated for each individual design.

The resulting system weights and efficiencies for the parameters specified in Table 3.3 (page 83) of the First Quarterly Report (26), are presented in Figures 2.3-11 to 2.3-18. The coolant average temperatures and coolant flow rates for system kva rating and input voltage parameters are presented in Figures 2.3-8, 2.3-9, and 2.3-10. The general trends of weight, efficiency and coolant flow rate versus the indicated parameters are established by these curves. Discontinuities exist in the coolant average-temperature data because the consideration of using available thyristors results in the use of several different types of thyristors for the range of kva rating and voltages shown in the figures. The different characteristics (e.g., operating temperature) of these components directly affect the coolant average-temperature calculations.

The coolant average temperature data shown on the figures does not include all of the parametric systems. The coolant average temperature and coolant flow rate data for the remaining systems are not presented graphically since their range of values is small; the coolant average temperature range is 139°F to 143°F, and the coolant flow rate range is 5.92 lb/min to 6.86 lb/min. These results are due to the fact that these systems are all of the same kva rating and voltage and consist of the same type and number of components.

Figure 2.3-8

Frequency Changer Parametric Data

Figure 2.3-9

Frequency Changer Parametric Data

Figure 2.3-10
Frequency Changer Parametric Data

2.3.5 Weight and Efficiency Calculation Results

Final results of the study are plotted in the curves of Figures 2.3-11 through 2.3-18. Little variation is noted in efficiency throughout the entire range of the study. Lowest efficiency results at the smallest rating (5 kva) where the input-to-output frequency ratio is smallest. For most practical ratings, the efficiency is in excess of 97 percent. As would be expected, the efficiency is lower in the 0.5 power factor case than with 0.7 power factor since the current and losses remain the same.

Weight increases, of course, with increased kva rating. As shown in Figures 2.3-11 and 2.3-12, the weight increase is rather mild with rating up to 100 kva but rapid above 100 kva. The primary reason for this is that the higher ratings require the use or paralleled thyristors and commutating inductors. The low weight increase with rating below 100 kva may be somewhat deceiving because it does not reflect the increase in coolant flow rate required in the system.

In the case of variable voltage ratings, Figure 2.3-13 shows a rapid increase in weight with increased voltage rating. The reason for this is that it is necessary to use thyristors in series to increase the voltage rating. Since the thyristors and associated hardware comprise the bulk of the weight, there is little weight economy with increased voltage rating so far as the cycloconverter is concerned.

Figures 2.3-14 through 2.3-17 show that weight decreases as the output frequency is increased up to approximately 400 cps. Figure 2.3-17 shows that weight decreases as the input frequency is increased. The reason for this variation is mainly that the weight of transformers in the control circuits decrease as their operating frequency increases.

Figure 2.3-11

Frequency Changer Parametric Summary

Figure 2. 3-12

Frequency Changer Parametric Summary

Figure 2.3-13
Frequency Changer Parametric Summary

Figure 2.3-14
Frequency Changer Parametric Summary

Figure 2. 3-15

Frequency Changer Parametric Summary

Figure 2.3-16

Frequency Changer Parametric Summary

Figure 2. 3-17
Frequency Changer Parametric Summary

Figure 2. 3-18

Frequency Changer Study Summary

2.4 Power Conditioning Study

2.4.1 Rectifier Designs

During this reporting period, parametric designs were made for the 1, 10, and 30 kilovolt d-c bus rectifier systems using silicon diodes. In addition, the 5 kilovolt silicon diode rectifier-system designs initiated in the first reporting period were completed. The design approach, design guidelines, and basic assumptions applied to the 5 kilovolt rectifier systems (documented in the First Quarterly Technical Progress Report) (26) were retained. Therefore, the three-phase full-wave bridge configuration was used for the 1, 10, and 30 kilovolt rectifier designs. The rectifier leg peak inverse voltage rating of 2.25 times the inverse voltage rating under normal operation and a rectifier current capability of 300 percent of normal rating for five seconds were used to determine the total quantity of diodes in the rectifier assemblies.

A. Progress to Date - Tables 2.4-1, -2, -3 and -4 list the electrical component identity and list the calculated quantities of weight, power losses, and conversion efficiency for the 1, 5, 10, and 30 kilovolt d-c bus rectifier system respectively. The parametric designs for the 1, 5, and 10 kilovolt rectifier systems have been completed according to the requirements specified in Table 3.5 (page 95) of the First Quarterly Technical Progress Report. The 500 and 1000 kilowatt designs remain to be completed for the 30 kilovolt system. The electrical quantities for the 5 kilovolt, 250 kilowatt rectifier assembly, presented during the first reporting period, have been updated to provide improved designs.

It is seen from the tables that several rectifier assemblies require parallel diodes in each of the rectifier legs to increase the current capacity. For example, the 10 kilovolt, 10 megawatt rectifier assembly uses four strings of parallel diodes per rectifier leg with 10 series diodes in each string. The transformer designs for these parallel diode rectifier assemblies will require only a single secondary winding. If, because of other considerations, the transformer secondaries must be increased such that four transformer secondaries would be required for the 10 kilovolt, 10 megawatt rectifier assembly, the rectifier system would then be made up of four separate three-phase full-wave bridges. The tabulated rectifier data as presented, however, would still be applicable.

In Table 2.4-3, the 1000 kilowatt assembly shown could have been designed with the Westinghouse Type 402Z diode using two parallel strings of diodes per rectifier leg with a total component weight of approximately 22 pounds. This design was rejected however, because the use of Westinghouse Type 790 diodes provides an assembly with less power loss and only 21 percent of the components.

Table 2.4-1 1 Kilovolt DC Silicon Diode Rectifier Assembly Systems

<u>DC Bus Power (kw)</u>	<u>25</u>	<u>75</u>	<u>250</u>	<u>500</u>
AC System Frequency (cps)	400 to 3200	400 to 3200	400 to 3200	400 to 3200
DC Bus Current (amps)	25	75	250	500
Qty. Rectifier Assemblies	1	1	1	1
<u>Diode:</u>				
Type	(W) 404Z	(W) 402Z	(W) 790	(W) 790
Amps./3-Phase Bridge	25	75	250	500
Amps./Bridge Leg				
Peak Amps	25	75	250	250
Avg. Amps	8.33	25	83.3	83.3
PIV (kv)	1	1	2.4	2.4
Series Diodes/Rect. Leg	3	3	1	1
Parallel Diodes/Rect. Leg	None	None	None	2
Total Qty. Diodes	18	18	6	12
Total Losses (watts)	175	504	750	1500
Total Weight (lbs)	0.28	0.68	3.75	7.5
<u>Resistor:</u>				
Type	Dale RS-10-39	Dale RS-10-39	None	None
Resistance (k-ohms)	250	200	None	None
Total Qty. Resistors	18	18	None	None
Total Losses (watts)	3.6	4.5	None	None
Total Weight (lbs)	0.36	0.36	None	None
<u>Capacitor:</u>				
Type	Tubular Bendix E-200	Tubular Bendix E-200	None	None
Capacitance (ufd)	0.01	0.01	None	None
Total Qty. Capacitors	18	18	None	None
Total Losses, Max. (watts)	1.76	1.76	None	None
Total Weight (lbs)	0.34	0.34	None	None
Total Component Weight (lbs)	0.98	1.38	3.75	7.5
Total Losses, Max. (watts)	180.4	510.3	750	1500
Rectifier Conversion Eff. (%)	99.28	99.32	99.7	99.7

Table 2.4-2 5 Kilovolt DC Silicon Diode Rectifier Assembly System

DC Bus Power (kw)	25	75	250	500	1000	5000
AC System Freq. (cps)	400 to 3200	400 to 3200	400 to 3200	400 to 3200	400 to 3200	400 to 3200
DC Bus Current (amps)	5	15	50	100	200	1000
Qty. Rectifier Assemblies	1	1	1	1	1	1
<u>Diode:</u>						
Type	(W) 404Z	(W) 404Z	(W) 402Z	JEDEC 1N1403	(W) 790	(W) 790
Amps./3-Phase Bridge	5	15	50	100	200	1000
Amps./Bridge Leg						
Peak Amps	5	15	50	100	200	250
Avg. Amps	1.67	5	16.7	33.3	66.6	83.3
PIV (kv)	1	1	1	0.6	2.4	2.4
Series Diodes/Rect. Leg	12	12	12	20	5	5
Parallel Diodes/Rect. Leg	None	None	None	None	None	4
Total Qty. Diodes	72	72	72	120	30	120
Total Losses (watts)	158.4	396	1190	4440	2970	15000
Total Weight (lbs)	1.13	1.13	2.7	22.5	18.8	75
<u>Resistor:</u>						
Type	Dale RS-10-39	Dale RS-10-39	Dale RS-10-39	Dale RH-25	Dale HG-50	Dale HG-50
Resistance (k-ohms)	250	250	200	15	60	60
Total Qty. Resistors	72	72	72	120	30	120
Total Losses (watts)	22.3	22.3	27.7	221	222	888
Total Weight (lbs)	1.44	1.44	1.44	3.43	1.86	7.44
<u>Capacitor:</u>						
Type	Tubular	Tubular	Tubular	Tubular	Rectangular	Rectangular
	Bendix E-200	Bendix E-200	Bendix E-200	Bendix E-200	Bendix E-200	Bendix E-200
Capacitance (ufd)	0.01	0.01	0.01	0.1	0.1	0.1
Total Qty. Capacitors	72	72	72	120	30	120
Total Losses Max. (watts)	18.7	10.9	10.9	65.4	174	696
Total Weight (lbs)	1.37	1.37	1.37	9.08	18.6	74.4
Total Component Weight (lbs)	3.94	3.94	5.51	35.01	39.26	156.84
Total Losses, Max. (watts)	199.4	429.2	1228.6	4726.4	3366	16584
Rectifier Conversion Eff. (%)	99.21	99.43	99.51	99.06	99.66	99.67

Table 2.4-3 10 Kilovolt DC Silicon Diode Rectifier Assembly System

DC Bus Power (kw)	500	1000	5000	10,000
AC System Freq. (cps)	400 to 3200	400 to 3200	400 to 3200	400 to 3200
DC Bus Current (amps)	50	100	500	1000
Qty. Rectifier Assemblies	1	1	1	1
<u>Diode:</u>				
Type	(W) 402Z	(W) 790	(W) 790	(W) 790
Amps./3-Phase Bridge	50	100	500	1000
Amps./Bridge Leg				
Peak Amps	50	100	250	250
Avg. Amps	16.7	33.3	83.3	83.3
PIV (kv)	1	2.4	2.4	2.4
Series Diodes/Rect. Leg	24	10	10	10
Parallel Diodes/Rect. Leg	None	None	2	4
Total Qty. Diodes	144	60	120	240
Total Losses (watts)	2375	3390	15000	30000
Total Weight (lbs)	5.4	37.5	75	150
<u>Resistor:</u>				
Type	Dale RS-10-39	Dale HG-50	Dale HG-50	Dale HG-50
Resistance (k-ohms)	200	60	60	60
Total Qty. Resistors	144	60	120	240
Total Losses (watts)	55.7	444	888	1776
Total Weight (lbs)	2.88	3.72	7.44	14.88
<u>Capacitor:</u>				
Type	Tubular	Rectangular	Rectangular	Rectangular
	Bendix E-200	Bendix E-200	Bendix E-200	Bendix E-200
Capacitance (ufd)	0.01	0.1	0.1	0.1
Total Qty. Capacitors	144	60	120	240
Total Losses, Max. (watts)	22	348	696	1392
Total Weight (lbs)	2.74	37.2	74.4	148.8
Total Component Weight (lbs)	11.02	78.42	156.84	313.68
Total Losses, Max. (watts)	2452.7	4182	16584	33168
Rectifier Conversion Eff. (%)	99.51	99.58	99.67	99.67

Table 2.4-4 30 Kilovolt DC Silicon Diode Rectifier
Assembly System

DC Bus Power (kw)	5000	10,000
AC System Freq. (cps)	400 to 3200	400 to 3200
DC Bus Current (amps)	166.5	333.3
Qty. Rectifier Assemblies	1	1
<u>Diode:</u>		
Type	(W) 790	(W) 790
Amps./3 Phase Bridge	166.5	333.3
Amps./Bridge Leg		
Peak Amps	166.5	166.5
Avg. Amps	55.5	55.5
PIV (kv)	2.4	2.4
Series Diodes/Rect. Leg	30	30
Parallel Diodes/Rect. Leg	None	2
Total Qty. Diodes	180	360
Total Losses (watts)	13750	27500
Total Weight (lbs)	112.8	225.6
<u>Resistor:</u>		
Type	Dale HG-50	Dale HG-50
Resistance (k-ohms)	60	60
Total Qty. Resistors	180	360
Total Losses (watts)	1340	2680
Total Weight (lbs)	11.16	22.32
<u>Capacitor:</u>		
Type	Rectangular Bendix E-200	Rectangular Bendix E-200
Capacitance (ufd)	0.1	0.1
Total Qty. Capacitors	180	360
Total Losses, Max. (watts)	1044	2088
Total Weight (lbs)	111.6	223.2
Total Component Weight (lbs)	235.56	471.12
Total Losses, Max. (watts)	16134	32268
Rectifier Conversion Eff. (%)	99.68	99.68

Table 3.5 (page 95) of the First Quarterly Technical Progress Report listed the three-phase rectifier systems to be evaluated in the parametric study. On the basis of the Power Uses and Mission Study it was concluded that the 20 volt rectifier systems would not be necessary since this voltage system would probably be intended for heater loads only. These loads can be supplied from the alternating current supply through a transformer.

In addition, the 20 kv rectifier parametric designs have been deleted since this information can be extrapolated from the 10 and 30 kv systems.

Preliminary information has been received on high temperature gas and vapor tube characteristics from General Electric. Additional information has been requested to clarify this data regarding transient voltage and short-circuit current capability. The tube devices represented by this data have not been fabricated. The General Electric information represents estimated tube characteristics based on existing high temperature tube technology as established on NASA gas and vapor filled thyratron and rectifier development contracts NAS 3-6005, NAS 3-6469, NAS 3-8525, and NAS 3-9423.

B. Rectifier Mechanical Design - Cooling of rectifier systems is by the cold plate method. The configuration and calculation procedures described in Section 2.3.4 (Mechanical Design - Frequency Changer) of this report also apply to the rectifier electrical system; the only difference being in the type and quantity of components. A conceptual sketch of this cooling method is presented in Figure 2.4-1. The system weights and efficiencies and the coolant average temperature and flow rate versus KW rating are presented in Figures 2.4-2 through 2.4-7 respectively. The parameters for these systems are specified in Parts 3, 4 and 5 of Table 3.5 (page 95) of the First Quarterly Technical Progress Report (26). These curves establish the trends of increasing weight, efficiency and coolant flow rate and decreasing coolant average temperature with increasing KW rating. The designs were made using only available components; as a result, a number of different components were required to cover the required range of parameters. The discontinuities in portions of the data are a result of this design limitation.

2.4.2 Transformer Designs

The transformer electrical and cooling design approaches selected during the first reporting period have been retained. Transformer designs are being made using high temperature materials and liquid potassium at transformer coolant temperatures of 500°F, 800°F, and 1100°F to be compatible with the other electrical system equipment. Single-phase and three-phase transformer designs are being calculated according to the requirements specified in Table 3.4 (page 87) of the First Quarterly Technical Progress Report (26).

Figure 2.4-1
Rectifier System Cooling

Figure 2.4-2
Rectifier Parametric Data

Figure 2.4-3

Rectifier Parametric Data

Figure 2.4-4
Rectifier Parametric Data

Figure 2. 4-5

Rectifier Parametric Data

Figure 2.4-6
Rectifier Parametric Data

Figure 2. 4-7

Rectifier Parametric Data

A. Progress to Date - The electrical portion of the computer design program is being prepared to accept the properties of different conductors, core materials, and insulations depending upon the coolant temperatures. These materials, identified in Table 2.4-5 show the maximum material operating temperatures with respect to the transformer coolant temperatures.

The materials required for the cooling system were selected and are listed in Table 2.4-6 with maximum operating temperatures for each transformer coolant temperature.

During this reporting period the transformer parametric design effort was directed to completing the electrical and cooling parametric design equations, introducing them into the existing design program, and establishing the iteration logic which will permit design refinement.

2.4.3 Plans for Coming Quarter

Effort in the coming quarter will be directed to completing the remaining silicon diode rectifier designs.

Based on the evaluation of the available gas and vapor tube data, rectifier systems using these devices will be limited to d-c bus voltages of 1, 5, and 30 kilovolts. The rectifier system power ratings will be selected after the additional tube information has been received. Electrical and cooling designs will be completed for the applicable gas-tube rectifier systems.

The parametric transformer design data will be generated in the form of curves relating the various electrical system parameters.

Table 2.4-5 Transformer Materials and Temperatures

Coolant Temperature (°F)	Maximum Operating Temperature (°F)	Material
500 800 1100	1200 1400 1600	<u>Conductors:</u> Inconel 600-Clad Silver Inconel 600-Clad Silver Dispersion-Strengthened Copper
500 800 1100	1100 1100 1400	<u>Magnetic Core:</u> Hipersil Hipersil Hiperco 27
500 800 1100	1200 1400 1600	<u>Rigid Insulation:</u> 99% Alumina 99% Alumina 99% Alumina

Table 2.4-6 Transformer Cooling System Materials and Temperatures

Coolant Temperature (°F)	Maximum Operating Temperature (°F)	Material and Function
		<u>Coolant Ducts & Cold Plates</u>
500	600	Tantalum T-111
800	900	Tantalum T-111
1100	1200	Tantalum T-111
		<u>Potting Compound</u>
500	1200	W-839
800	1400	Boron Nitride Compound
1100	1600	Boron Nitride Compound
		<u>Insulation</u>
500	1200	Beryllia
800	1400	Beryllia
1100	1600	Beryllia

2.5 Transmission and Distribution Study

ABSTRACT

Although the relationships of the transmission and distribution parameters are quite complex, an effort to develop a mathematical relationship between all parameters is being carried out. In the transmission line study to date, definite trends have been established and are being evaluated. Representative contactor designs were determined from past studies and are presented in the form of curves relating the various parameters.

2.5.1 Transmission Lines

Although transmission line performance depends on the interaction of many parameters, limitations must be set for the evaluation and possible combinations of parameters. Data points for the parametric evaluation are based on the ranges in Table 2.5-1. An approach to the parametric study is desired which will aid in the recommendation of systems parameters. In order to accomplish such a task, use of the computer was found to be necessary. Two Westinghouse computer programs were available for the parametric study of transmission lines and were combined to accomplish the required task. The combined program contains the following assumptions:

- a. hollow or solid round conductors
- b. high emissivity = 0.97
- c. radiant cooling
- d. equal cooling throughout all 360° of conductor
- e. the amount of conductor used at frequency greater than (0) zero, is equal to the nominal depth of penetration of current, or skin effect.

Data from References 9, 10 and 11 were utilized in combining the two programs into a common one.

The evaluation was separated into three separate tasks which are (1) an electrical parameter evaluation, (2) a configuration evaluation, and (3) a cooling evaluation. The computer program is being used primarily for the electrical parameter evaluation but also has features compliant with parts of both the configuration evaluation and the cooling evaluation.

A. Electrical Parameter Evaluation

1. Choice of Radius Ratio - Assuming a hollow round copper conductor, a radius ratio that provides good performance relative to skin effect was selected. The selection of this ratio was based on the optimum of weight versus losses for any given frequency. Table 2.5-2 gives a representative sample of radius ratios calculated.

Table 2.5-1 Parameter Ranges and Data Points for Transmission and Distribution Evaluation

Frequency	0, 50, 100, 200, 400, 800, 1600, 2000, 3200 cps.
AC Voltage	120, 240, 500, 1000, 2000, 5000, 10000, 20000 volts, 3-phase line-to-neutral.
DC Voltage	100, 200, 500, 1000, 2000, 5000, 10000, 20000, and 30000 volts.
Transmission Distance	25, 50, 100, 200, 300, 500 feet.
Current	50, 100, 200, 300, 400, 500 amperes.
Temperature (coolant or conductor)	500°F, 800°F, 1100°F

Table 2.5-2 Radius Ratio - Relating Skin Effect to Radius Ratio

Weight lbs/ft	0.1	0.15	0.2
frequency (cps)	Ro/R _I		
3200	1.01	1.01	1.01
2000	1.02	1.01	1.01
1600	1.02	1.01	1.01
800	1.04	1.03	1.02
400	1.09	1.06	1.04
200	1.18	1.12	1.09
100	1.39	1.24	1.18
50	1.97	1.56	1.39
D.C.	∞	∞	∞

2. Weight Versus Losses and Other Combinations of Parameters

Plots of weight (WTL) versus losses (TWS) per unit length as a function of current for each frequency, based on the conductors specified for A.1. above, are required to show best possible frequency selection based on weight and losses. Figures 2.5-1 and 2.5-2 show that the best choice of transmission lines would be of high frequency, e.g., 3200 cps.

Figure 2.5-3 shows a plot of weight versus losses as a function of frequency for each current. This graph shows the preference for low current and high voltage for optimum weight versus losses. For light weight conductors, the difference in losses at a given weight can be as high as 1000 to 1 which is extremely significant. The combination of trends for the above parametric plots show high frequency low current as the ideal combination of parameters as one would expect.

Of importance also is the voltage drop per unit length versus frequency as a function of current. Figure 2.5-4 shows only 50 amps compared with 100 amps; however, again it is obvious that high frequency and low current produce the best performance characteristics for transmission lines.

B. Configuration Evaluation - Evaluation is not complete in this area; however, preliminary steps have been taken and the following conclusions and/or steps made.

1. Comparison of Hollow Round Conductor to Flat Conductor -

The primary advantage of flat wire is to increase the surface area of the conductor for cooling purposes. Therefore, slightly higher currents can be carried for the same weight, assuming constant current density as in round conductors. However, skin effect tends to increase current density in the corners or edges of flat conductors thereby causing hot spots and uneven stress on the conductor and insulating materials. This leads to wasted materials and little or no gain in the weight versus losses. Therefore, flat conductors do not appear to be of any great advantage; these points shall be considered further during the next quarter.

2. Special Recommendations - The special recommendations required under the configuration evaluation deal primarily with spacing or positioning of conductors. For low current levels, spacing is not generally a problem; only for high current does proximity effect become pronounced. Therefore, if previous recommendations to run on low current are followed, the conductors may be spaced as desired either for space saving purposes or for temperature requirements of the conductor.

3. Comparison of Copper and Aluminium - Complete evaluation of copper has not yet been accomplished. Until that time, evaluation of aluminium conductors and their eventual comparison with copper cannot be performed.

Figure 2.5-1

Weight versus Losses as a Function of Frequency for 50 Amps Current

Figure 2. 5-2

Weight versus Losses as a Function of Frequency for 400 Amps Current

Figure 2.5-3

Voltage Drop per Unit Length versus Frequency for a Specified Conductor Size
Wt = 0.1 lb/ft

Figure 2.5-4

Weight versus Losses as a Function of Current for 2000 cps Frequency

4. Insulation Requirements - Only limited work has been done in this area. This evaluation will be finished after the electrical parameter evaluation has been completed and the conductor sizes have been established.

C. Cooling Evaluation - Analytical test points have been selected to determine what advantage or disadvantage liquid cooling has over pure radiation cooling. Discussions of cooling approaches lead to the conclusion that additional cooling will be of no great advantage because of the added weight and lack of inherent reliability associated with active cooling. Analysis will be made to amplify these points and show the weight penalty caused by active cooling.

2.5.2 Contactor Design Evaluation

A. Performance Requirements - Contactors are required in a power system to provide protection against continued faults or overloads, to permit manual or remote application and removal of power to a load bus, and to permit transfer of power between buses. When coupled with automatic devices to sense an overload and trip the contactor, they provide the function of a circuit breaker.

Depending upon the required function and location in the system, these contactors will be required to carry and interrupt varying portions of the system capacity in 0.25 to 10 megawatt space power systems. This power in turn may be a-c or d-c at various voltage, current, and frequency levels. It is necessary in this evaluation, therefore, to consider a wide range of ratings of both a-c and d-c contactors.

From an analysis of the power uses and missions it has been found that a main three-phase contactor may be needed to carry the complete generator output. Thus, maximum ratings of the order of 10 MVA must be necessary to interrupt this power. Since such action would remove all power from the buses, it may be desirable to first interrupt the generator field to reduce the amount of output current that must be interrupted.

Besides covering the range of system capacities of 0.25 to 10 MW, smaller ratings must also be considered for use in supplying separate loads or buses. Contactors as small as 10 kva may influence the system parameter selection if used in sufficient number.

Frequencies to be considered include the low values that may be supplied to electromagnetic pumps up to the maximum (potential) values of generated frequency. Thus, for the a-c contactors, a frequency range of 50 to 3200 Hz must be considered.

Voltage ranges that must be considered include high voltages for possible transmission requirements and for high-voltage loads such as radar or electric engines. AC voltages to be considered include 100 volts up to approximately 20 kilovolts. Lower a-c voltage loads will very likely not require separate contactors. DC voltages on the other hand must be considered for the range of 20 volts up to 30 kilovolts according to the load requirements of the power uses and missions analysis.

Since the weight penalties of a spacecraft cooling system are very substantial, it is desirable that the contactors be capable of operating at high temperature with very low losses. For use in a power system with a nuclear reactor energy source, radiation damage effects to the contactors must also be considered.

For most applications, latching type contactors are desirable to conserve power and provide fail-safe operation. These latches may be of either the permanent magnet or the mechanical type.

B. Comparison of Types - Of the wide variety of contactors available, classification may be made into three types: vacuum, gas-filled (including air) and oil-filled. Oil-filled contactors are relatively heavy and offer no offsetting advantages for spacecraft use, especially for operation at higher temperatures.

Vacuum contactors provide a high dielectric while requiring a very short gap in order to provide current interruption. These devices provide dielectric strengths⁽¹²⁾ of up to 5000 volts/mil with vacuums of 10^{-4} to 10^{-7} mm Hg. This corresponds to an altitude of approximately 70 to 200 miles. Thus, for space applications, reliable operation can be obtained without the need for seals to withstand ambient pressures over long periods in order to maintain the vacuum. In fact, if the system is to be started in space, it may be possible to eliminate the need for the seals altogether. Extinction of the arc in vacuum contactors is aided by the fact that very few ions exist to maintain the arc.

A development program⁽¹³⁾ for space switchgear has shown that the presence of radiation will tend to increase the number of ions in the gas filled units to a level sufficient to cause reignition in high-voltage (above 1 kilovolt) switchgear. This program also determined that radiation damage to structural elements in high-temperature switchgear is not of great concern for most potential applications.

1. Vacuum Contactor Technology - Vacuum contactors to interrupt large amounts of power have been developed primarily for commercial applications. They have been designed to interrupt a-c lines where the current passage through the zero-point aids in extinguishing the arc. Existing d-c designs have been limited to a maximum current interruption of about 10 amperes at high voltages because larger currents tend to maintain the arc and require large contact areas that are more prone to welding. At very low voltages (under 20 volts), d-c currents of up to 350 amperes have been interrupted.

For commercial applications, a-c vacuum contactors have seldom been used for applications below 5000 volts simply for economical reasons. At the lower voltages, less costly gas or oil filled units are used. For spacecraft, however, the vacuum devices promise a high reliability and they are relatively light in weight.

An analysis of vacuum contactor state-of-the-art by H. C. Ross⁽¹⁴⁾ indicates that a-c vacuum switch technology has been developed for opening 1200 amperes continuous and 12000 amperes maximum interruptive current. At low current levels they have also been built for up to 300 kilovolts at 10000 Hz. DC devices have been designed for 240 kilovolts for relatively low-currents.

Most designs utilize tungsten or molybdenum butt-type contacts encircled by a metal vapor-condensation shield. The chosen contact materials have been found to be resistant to contact welding and possess a low vapor pressure to starve ions from the arc. Copper contacts are more prone to welding and their high vapor pressure permits a large amount of contact vaporization at relatively low arc temperatures. The vapor shields condense metal vapor to prevent its condensing on insulators and making them conductive.

In interrupting a-c currents, it is preferable to chop the arc at a relatively low value of current to reduce transients. In his paper, Ross⁽¹⁴⁾ states that for medium-sized (about 1/2 inch) tungsten contacts spaced at 5/32 inch, arc voltage is approximately 21 volts for a few hundred amps to 32-35 volts in the 5000 amp range. The chopping current-level is dependent upon the contact opening speed. Lower speeds result in reduced chopping current levels.

Current capability of the contacts determines the required contact size and the contact size governs the actuator design and likelihood of welding. For simple butt-type tungsten contacts maximum current values are approximately 3500 amps for 0.5 inch diameter, 6500 amps for 1.0 inch diameter, 12000 amps for 2.0 inch diameter, and 20000 amps for 3.0 inch diameter contacts. These limits can be increased by varying the contact configurations and by arc-spinning magnetic fields, but a certain amount of contact derating would also be desirable.

2. Advanced-Design Vacuum Contactor Development - Under NASA Contract, General Electric⁽¹³⁾ has been evaluating the requirements for applying vacuum contactor technology to high-temperature spacecraft applications. The main objectives of this effort are to investigate contact materials, demonstrate interruption of high-frequency a-c at high operating temperatures, and demonstrate interruption of high voltage d-c at high temperatures.

This program is intended to produce an a-c contactor capable of interrupting 1800 amperes at 1500 volts, 2000 Hz and a d-c contactor capable of interrupting 20 amperes at 10000 volts d-c. A brief conceptual design study of a 100 volt d-c, 10000 ampere contactor was also made. Design objective is for operation of these units in ambient temperatures of 538°C to 700°C.

Test results at 600°C indicated a capability to interrupt 4300 amps peak at 2200 volts, 2000 Hz a-c and also 10800 volts, 21 amps d-c. Tungsten butt-type contacts were selected at 1 inch diameter for a-c and 1/2 inch for d-c.

The single-phase a-c and the d-c interrupter designs are similar. However, the a-c contactor would require an interrupter for each of the phases. Actuator designs were found sufficient to

break the slight weld occurring at the contact faces. A certain amount of welding will occur in all cases where contacts of very pure material are held together for long periods of time in a vacuum. This condition is even worse for high temperatures or for closing on high currents. Welding can be reduced by proper control of the closing speed.

Tests on both a-c and d-c contactors indicated that 1/4 inch contact separation was sufficient for arc interruption. The a-c unit was intended for operation at 1000 to 3000 Hz. Testing showed interrupting performance at 2000 Hz to be superior to 60 Hz where most commercial designs operate. At the higher frequency levels, skin effect would have permitted removing the center of the contact but it was left in for thermal reasons in the units fabricated for testing.

Current through the contacts tends to separate them. A closing force of 50 pounds was found to be needed for the a-c contactor and 20 pounds for the d-c. This may be compared to the estimate of 400 pounds force needed for the four inch to five inch diameter contacts of the 100 volt, 10000 ampere d-c contactor conceptual design. Hydrogen gas in the 10000 ampere contactor serves to prevent cold welding and provides a supply of gas for a pneumatic actuator.

A review of follow-on efforts (15) of this development indicate that the single-phase a-c contactor will occupy a volume of approximately 8-1/2 inch diameter by 23 inches long. The actuator will weigh about 25 pounds. The actuator for the high-voltage d-c unit is required to be about the same size when compared to the a-c unit. The overall size for the d-c unit would be three inches to four inches shorter in length than the a-c unit and slightly smaller in diameter. The interrupter sections of the contactor can be estimated to weigh between two and five pounds. A three phase a-c unit would be approximately three times the size and weight of the single-phase development unit for the same line current.

Ross (14) states that a single pair of simple 1-inch butt-type contacts such as have been used in commercial vacuum interrupters are presently limited to interrupting 4000 amperes rms at 18 kilovolts rms, 60 Hz. This represents an interruption of 72 mva. He estimates that with differing contact materials, contact configurations, and interruption techniques, this capability may increase by as much as a factor of 10.

3. Technology of Gas-Filled Contactors - There have been a number of lightweight, gas-filled contactors designed and utilized for aircraft and space craft operation. In general, these have not been designed for the high temperatures or the large capacity needs that would occur in nuclear electric systems for space. There have also been designs of slightly larger power rating for terrestrial applications which, though not optimized for low weight, still represent the state-of-the-art of interruption capacity. For these existing designs, the chief area of concern is in the high current capacity d-c devices. This is because the vacuum contactor

technology for these devices, which require large contact area, is relatively limited. However, the three-phase a-c designs are also of interest since they represent demonstrated performance levels in adverse environments. These a-c designs may also prove to be lighter and smaller than vacuum contactors for those areas of the system requiring reduced voltage levels or load capacity.

For both a-c and d-c requirements, several typical designs will be considered as representative of present technology.

(a) AC Contactors - An existing three-phase a-c contactor built by Westinghouse for aircraft applications provides for 265 ampere continuous operation and fault interruption of 4000 amperes. This 3PST* contactor (Type AVB-36) is for 115/200 volt, 400 Hz operation and thus represents a continuous power capacity of up to 90 kva. This unit has six auxiliary contacts, weighs 4.3 pounds and occupies a volume of 84 cubic inches. It is a magnetic latching device.

A higher capacity 3PST unit is made by Hartman Mfg. (Type B-251B). Also for 115/200 volt, 400 Hz applications, it carries 700 amperes continuously and will interrupt 3500 amperes. Thus, the maximum continuous capacity is 240 kva. Maximum weight is 18 pounds with a volume of 430 cubic inches.

These two designs are typical except for the fact they are not hermetically sealed for operation in a vacuum environment; hermetic sealing could be added with a moderate increase in weight.

(b) DC Contactors - Existing designs of d-c contactors provide for interruption of up to 1500 amperes at 30 volts. Other units operate with somewhat less current and higher voltage. Most of the higher capacity units have not been designed for very low weight simply because contactors of this type have not as yet been needed for spacecraft.

The Hartman Type A-751Y contactor is a 165 volt d-c, 650 ampere electrically held unit. Thus, it is capable of carrying 107 KW but the rated duty is only for 10 minutes. Arcing contacts are used to gain a minimum life of 500 cycles. Volume of the unit is approximately 113 cubic inches with a weight of 8 pounds.

The Leach Type 7401-5218 is designed for 400 amps at 120 volts d-c of 48 KW. This unit weighs 2.6 pounds with a volume of 30 cubic inches.

The Hartman A890F contactor is rated at 20 volts d-c, 2000 amperes but is not hermetic sealed nor optimized for low weight. It weighs 47 pounds with a volume of 0.75 cubic feet. This unit carries 40 KW and is capable of interrupting 10000 amperes for several cycles.

Higher values of current at d-c voltages could obviously be obtained through a development effort. This development should also concentrate on relatively high temperature operation to reduce cooling penalties.

*3PST = 3-pole, single throw

C. System Considerations

1. AC or DC Contactors - In the application of switchgear to space nuclear electric power systems there are a number of considerations affecting the selection of contactors and circuit breakers and the location of them in the overall system. For the systems being considered, it has been determined that a large percentage of the power will be used in the form of direct current. Thus, for the protection of a d-c bus, the switchgear could be placed on the d-c or on the a-c side of the transformer-rectifier. By placement on the a-c side a fault in the transformer-rectifier could also be interrupted. This approach has the disadvantage in that the complete bus is isolated by interruption and if there is more than a single d-c load on the bus, the a-c contactor may have to be supplemented by or replaced by d-c contactors on the individual loads. With this approach, a faulted load would result in removal of only that load from the bus rather than the complete bus.

If the d-c load is large and at a low voltage, it may necessitate a contactor on the a-c side because of the problems of building contactors for high values of d-c current. Similarly, for loads of high voltage d-c, a smaller contactor may be sufficient if it is inserted on the d-c side or between the transformer and rectifier rather than in the low voltage a-c.

2. Main Contactor - The use of a single main contactor carrying the load may or may not be desirable depending upon the system design. A short bus section at the generator output may be well protected physically, splitting into several feeders, each with its own contactor. However, for multiple generator systems, especially when paralleled or capable of bus-transfer, a single large contactor will be needed to permit isolating a given generator.

Although this contactor must carry a large quantity of power, the amount of fault current that must be interrupted can usually be limited by a contactor in the generator field. Thus, opening of the field of a given generator will permit subsequent opening of the main contactor with little more than residual current. With this arrangement, however, a large amount of power may still have to be interrupted by contactors connecting the separate buses. That is, in a paralleled system with one faulted bus, contactors must prevent the paralleled generators from feeding the fault without total interruption of power in the system.

3. Reliability in a Vacuum - It first appears that, when operating in a vacuum, loss of the hermetic seal on a gas-filled contactor could quickly result in loss of interruption capability through the welding of contacts. On the other hand, loss of the seal of a vacuum contactor appears to be of relatively little concern.

However, consideration must also be given to the fact that these contactors are using interrupters evacuated to an equivalent altitude of approximately 200 miles or less. For operation deeper into space, the ambient vacuum could be sufficient to increase the amount of contact welding that would occur.

Thus, even the vacuum contactors must either be provided with a reliable seal or must be provided with actuators designed to provide interruption in the ambient vacuum where they are to be operated, regardless of their initial vacuum level.

4. Derating Requirements - When operated in a system, the switchgear must be derated well below its maximum capabilities to provide for high reliability and longer life as well as meeting fault and transient requirements. There is generally a direct relationship between the amount of current that is interrupted and the number of cycles before failure of the unit through damage to the contacts. Therefore, a unit capable of interrupting a current of 10000 amperes for 10 cycles would be required to interrupt, for example, only 1000 to 2000 amperes under normal conditions. This would result in a lifetime of many more cycles and would also provide a safety factor in the event of a fault or severe overload.

In applying the contactors to a particular mission and system design, the required duty must be determined. Estimates must be made of the number of operating cycles in meeting each function and the amount of current to be closed upon and interrupted. Continuous use of high capacity contactors in a spacecraft application would certainly not require the number of operating cycles normally specified for aircraft where the system is started and shut down very often. Therefore, it may not be necessary to provide the amount of derating normally specified for these devices provided the maximum fault current does not exceed contactor capability.

Similar problems arise in attempting to apply commercial vacuum contactor technology to spacecraft requirements. These devices are often designed to stand-off a much higher voltage than that under which they normally operate. This degree of conservative design may not be required for spacecraft applications. Thus, it can be seen that serious consideration must be given to the amount of required derating.

D. Parametric Evaluation - The parameter which exerts the most influence on contactor design is generally the current level. This influences the contact size which, because of the tendency of contacts to weld, in turn influences the actuator weight and volume. Current level also determines the amount of pressure that must be exerted by the actuator to hold the contacts closed.

By far the largest amount of losses, even in electrically held units, are contact losses whose magnitude is dependent upon the current level and contact resistance. Current level, however, is not a good evaluation parameter for parametric curves which are used to compare and select system parameters (such as voltage level). Since the function of these contactors is to deliver power to the load,

each comparison curve should be for a fixed power level. Thus, with a known power level to be handled, these curves permit the selection of the best combination of parameters to provide that amount of power.

For this reason, the evaluation of weight, volume and losses of contactors below presents each of these parameters as a function of voltage to deliver a specified level of load. This information may then be used in the system evaluation to select the desired combinations of voltage and current to supply the loads.

Since a-c contactors are provided with the advantage of current passage through zero to help in extinguishing the arc, they are inherently able to interrupt more power with a given size and weight than d-c contactors. In addition, d-c contactor development for aerospace applications has been limited well below the capacity of a-c contactors. Therefore, wherever a choice is possible, a-c contactors are desirable to carry the high power levels needed for space nuclear electric power systems. For these reasons, the parametric comparisons made below are for three-phase a-c contactors. Where d-c contactors must be used, the previous discussions will serve as an indication of present technology.

1. Graphical Comparisons - The weight, volume, operating temperature, and losses are the contactor parameters that should be known for evaluation of any system application. However, these factors can vary substantially depending upon the system specifications. The contactors may be electrically held or they may be of the latching type. The latching units may be magnetically held or there may be mechanical latches of varying types. There are also various actuators that may be used depending upon the required opening and closing speed and force as well as such items as shock and vibration requirements. Obviously these can cause wide variations in weight and volume. Similarly, the selected contact design and material can result in variations in the losses as can the coil design for electrically held units.

Despite these facts, an analysis of a single selected configuration will result in the determination of trends that hold true for other configurations. Figures 2.5-5 through 2.5-16 represent parametric comparisons of weight, volume, and losses as a function of voltage for various power levels and ambient cooling temperatures. This data is based on the use of permanent magnet latch, three-phase a-c, vacuum contactors. The values are the result of an evaluation of the study design efforts contained in References 1 and 21. In addition, Figures 2.5-6 and 2.5-10 contain estimated weights and volumes of a 2 mva, 600°C ambient design. These estimates are based on the design of a single phase unit as reported in Reference 15. The data for voltages above 5 kilovolts are also estimated values for three-phase units based on existing single-phase commercial designs as presented in Reference 12. Thus, the high-voltage units are based on relatively low operating temperatures and may require additional weight for structural support to meet an environment of severe shock, vibration and g-loading.

Figure 2.5-5
Weight Vs. Voltage for 3-phase, AC PM Latch Vacuum Contactors. 100°C Ambient

Figure 2.5-6

Weight Versus Voltage for 3-Phase, A. C. P. M. Latch Vacuum Contactors,
100°C and 500°C Ambient

Figure 2.5-7

Weight Versus Voltage for 3-Phase A.C. P.M. Latch Vacuum Contactors, 10,000 KVA,
100°C and 500°C

Figure 2.5-8
Weight Versus Voltage for 3-Phase, A. C. Vacuum Contactors,
100°C Ambient, 1000 KVA

Figure 2.5-9
Volume Vs. Voltage for 3-Phase AC P.M. Latch Vacuum Contactors. 100°C Ambient

Figure 2.5-10

Volume Vs. Voltage for 3-Phase AC PM Latch Vacuum Contactors. 100°C and 500°C Ambient

Figure 2.5-11

Volume Vs. Voltage for 3-Phase AC PM Latch Vacuum Contactors. 10MVA, 100°C and 500°C Ambient

Figure 2.5-12

Volume Vs. Voltage for 3-Phase Vacuum Contactors. 10,000 KVA, 100°C Ambient

Figure 2.5-13

Losses as a Function of Voltage for 3-Phase AC PM Latch Vacuum Contactors.
100°C Ambient

Figure 2.5-14

Losses as a Function of Voltage for 3-Phase A. C. P. M. Latch Vacuum Contactors,
100°C and 500°C Ambient

Figure 2.5-15

Losses as a Function of Voltage for 3-Phase AC Permanent Magnet
Latch Vacuum Contactors, 10 000 KVA,
100°C and 500°C Ambient

Figure 2.5-16

Losses as a Function of Voltage for 3-Phase P.M. Latch Vacuum Contractors,
10MVA, 100°C Ambient

Figure 2.5-5 indicates the weight as a function of voltage for units of 50 to 2000 kva. At 50 kva, the variation of weight with voltage is less pronounced. As power level increases at the low voltages, weight increases rapidly. This is because the very high current levels make the contacts larger as well as the actuator design needed to hold these contacts closed and open. The values of Figure 2.5-5 are based on a cooling ambient of approximately 100°C.

Figure 2.5-6 shows similar weight values for 2 mva and 4 mva and also gives comparative differences for 100°C and 500°C ambient. Figure 2.5-7 provides the same evaluation for 10 mva units and again shows the extreme increase in weight needed to provide for interruption of the high current levels. At the high ambient temperatures, increased weight is also needed for contact cooling. Figure 2.5-8 is intended to show the relative merits of using a very high voltage in the system and 10 mva was arbitrarily chosen as a point of comparison.

Figures 2.5-9 through 2.5-12 provide volume comparisons corresponding to Figures 2.5-5 through 2.5-8 respectively. Similar trends appear as increased volume is needed for structure to cool the contacts and provide interruption at the low voltage, high current values.

Figures 2.5-13 through 2.5-16 show the relative losses as a function of voltage corresponding to the weights and volumes of Figures 2.5-5 through 2.5-12. These assume the use of tungsten contacts. The loss values could be modified somewhat through variations in contact design and materials.

2. Effects of Other Parameters - The previous discussion of vacuum contactors has pointed out that tests at 2000 Hz have resulted in improved interruption capabilities over 60 Hz operation. Even at the lower frequencies, the vacuum contactor operation will depend upon the dielectric recovery of the gap rather than withstanding arc heating until the first current-zero is reached as is done for gas-filled contactors. Thus, the choice of system frequencies up to the maximum considered value of 3200 Hz will not significantly affect the design and weight of the contactors.

Although most existing contactor designs would not operate satisfactorily at high temperatures such as 500°C, development work is presently being conducted to provide this capability. The design estimates of high-temperature, high-capacity units are based on the assumption that the high-temperature technology will be proven and available. The feasibility of building contactors utilizing materials to meet these temperatures has been proven. It remains to provide testing to verify long cycle life and high capacities.

Since in the parameter ranges presently being considered, current is most significant in determining contactor size and weight, the value of voltage will prove to be significant primarily as it affects the current value. This is because the vacuum contactors provide a very high dielectric capability in contact separation distances of only a small fraction of an inch.

3. Contactor Cooling - Cooling must be provided for the contactors during both steady-state and transient-interruption conditions. For latching contactors, the major heat loads are the I^2R losses at the contact faces with some losses in the current carrying conductors. For electrically held units, a large amount of loss can also occur in the coil.

In the designs evaluated, it was assumed that the contactors would provide for conduction of the heat from the contacts to the contactor structural base where it would be removed to a cold plate or coolant tubes. An electrical insulating medium of high thermal conductivity such as beryllium oxide would have to be provided in the thermal path from the contacts. Since operation would very likely be in a vacuum, the only other heat transfer mode would most likely be by radiation.

E. Conclusions and Recommendations - From the foregoing analysis it can be seen that the technology is available to provide contactors and circuit breakers that will interrupt a-c and d-c voltages exceeding 30 kilovolts. At the higher voltages, this technology has been developed on commercial, single-phase vacuum units. Therefore, even with the existing technology, the application of these devices to a space nuclear electric power system will require significant development effort to produce three-phase units capable of withstanding the severe launch and operational environments that may be encountered. In addition, design consideration must be given to the aspects of how these devices will operate and function as a part of a space system.

Interruption of a-c power up to at least 10 mva can also be accomplished by design efforts based on existing technology. The interruption of d-c power is limited to about 1 MW, however, without additional development and testing, and at low voltages the capacity must be well below this level. This is because of the large contacts that are needed with their associated welding problems as well as the problem of arc extinction that is not aided by passage through a current-zero as in the a-c units.

Operating frequency of the a-c units does not appear to present significant problems in the ranges being considered. The curves of Figures 2.5-5 through 2.5-16 show the savings that can be obtained, at least at the higher power levels, by operating voltages approaching 2 kilovolts. Operation at coolant temperatures approaching or exceeding 500°C increases the contactor weight but may well result in offsetting reductions in cooling penalties. The development of technology for operation at these temperatures is presently being pursued under NASA contract.

In general, it can be stated that the high-capacity contactors should be placed in the system so that a-c power is to be interrupted rather than d-c. Where d-c power is to be interrupted, consideration should be given to split buses or individual load contactors; these should then be compared to the development problems and weight of a single high-current-capacity d-c contactor.

The primary area requiring additional development appears to be the lower-voltage, higher-current d-c contactor. Since much of the load is in the form of d-c, the individual load contactors must still carry a larger amount of power than the capacity of contactors presently available. They must also have sufficient derating to provide long life and high reliability. Thus, high-temperature contactors need to be developed to provide continuous ratings of at least 100 KW at voltages of 20 to 150 volts d-c.

2.5.3 Plans for Coming Quarter

During the coming quarter, effort will be directed toward completion of the Transmission Line evaluation in the following steps:

- a. Completion of evaluation of copper conductors.
- b. Comparison of copper to aluminium
- c. Insulating evaluation
- d. System recommendations based on above and system requirements.

The contactor parametric evaluation is complete.

3. SYSTEM DEFINITIONS

3.1 Electrical Loads Definition

ABSTRACT

An analysis of load requirements is made for various orbital, interplanetary, and lunar base missions requiring electrical power of 0.25 to 10 megawatts. This analysis is based on an evaluation of recently completed mission studies. It was determined that the majority of power is utilized in the form of d-c, but that the relative percentages and voltage values are very much dependent upon mission objectives.

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

3.1.1 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.

The contract statement of work requires that consideration be given to three classes of missions: interplanetary (electrical propulsion), manned earth orbital, and lunar or planetary base. Parameters to be evaluated include the power range, a-c and d-c 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 16, 17, 18, 19, 20, 21 and 22. These include 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.

3.1.2 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 16, 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 d-c
12 KW	-6600 volts d-c
208 KW	20 volts d-c
4.5 KW	30 volts d-c
2.0 KW	28 volts d-c

b. Radar

154 KW	20000 volts d-c
2.0 KW	28 volts d-c

c. Communications

376 KW	20000 volts d-c
4.0 KW	28 volts d-c

d. Circulation pumps (electromagnetic)

75 KW	120 volts, 3 $\emptyset$, 167 cps
-------	------------------------------------

e. Guidance and controls

8.5 KW	28 volts d-c
1.0 KW	26 volts, 2 $\emptyset$, 400 cps

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 cps. This no doubt affected the choice of the pump input voltage.

2. Manned Interplanetary Mission - A previous study (17) 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 a-c and 25 percent d-c. The d-c power is required at a nominal 28 volts to meet standard input requirements for the various items of equipment. The a-c 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.	Circulation pumps	510 KW
e.	Guidance and controls	10 KW
f.	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 (18), (19) have indicated maximum peak loads of 20 to 40 kilowatts. However, these studies were predicted on a very early initial launch data 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 (20), 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 thrusters (arc jets)
 - 240 KW at 130 volts d-c (4 channels)
 - 4 KW at 120/208 volts, 3 $\emptyset$, a-c
- b. Radar
 - 28 KW at 15 kilovolts, d-c
 - 12 KW at 10 kilovolts, d-c
 - 14 KW at 5 kilovolts, d-c
 - 14 KW at 28 volts, d-c
 - 4 KW at 120/208 volts, 3 $\emptyset$, a-c
- c. Communications
 - 14 KW at 8 kilovolts, d-c
 - 2 KW at 5 kilovolts, d-c
 - 1 KW at 2 kilovolts, d-c
 - 1.5 KW at 1 kilovolt, d-c
 - 1.5 KW at 28 volts, d-c
- d. Circulation pumps (estimated value)
 - 28 KW at 3 $\emptyset$, 120/208 volts, a-c
- e. Guidance and controls
 - 2.5 KW at 28 volts, d-c
 - 2.5 KW at generated voltage a-c (partially transformed and rectified internally)
- f. Generator excitation (estimated value)
 - 8 KW at generated voltage
- g. Life support
 - 1.8 KW at 28 volts, d-c
 - 4.2 KW at 3 $\emptyset$, 120/208 volts, a-c
- h. Laser illuminator
 - 119 KW at 3 kilovolts, d-c
 - 1 KW at generated voltage a-c (transformed and rectified internally)

i. Sensors and other loads

30 KW at 10 kilovolts, d-c
5.0 KW at 120/208, 3 Ø, a-c
11 KW at generated voltage a-c (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 d-c power at a much higher voltage.

Since one assumption in this particular mission analysis was that the generated power was three-phase a-c at 2040 volts line-to-neutral and 3200 cps, the majority of loads utilizing the generated voltage and frequency provide internal transformation and rectification. The choice of three-phase a-c 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 300-350 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 a typical mission are as follows:

a. Electric thrusters (arc jets)

510 KW at 130 volts d-c (6 channels)
12 KW at 120/208 volts, 3 Ø, a-c

b. Communications

45 KW at 30 kilovolts, d-c
25 KW at 20 kilovolts, d-c
25 KW at 10 kilovolts, d-c
10 KW at 3 kilovolts, d-c
35 KW at generated a-c (transformed and
rectified internally)

c. Circulation pumps (estimated value)

41.5 KW at 3 Ø, 120/208 volts a-c

d. Guidance and controls

2.5 KW at 28 volts d-c
11.4 KW at generated a-c (transformed and partially rectified internally)

e. Generator excitation (estimated value)

12 KW at generated voltage

f. Life support

1.8 KW at 28 volts d-c
4.2 KW at 3 ϕ , 120/208 volts a-c

g. Sensors and other loads

30 KW at 10 kilovolt dc
6.2 KW at 120/208 volts, 3 ϕ , a-c
5.8 KW at generated a-c (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 d-c voltage. For those loads not requiring a high d-c 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⁽²¹⁾ that a first-generation lunar base of 6 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 d-c 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. The study of lunar base

power systems 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 (1-10 percent).

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

a. Life support

9 KW at 28 volts, d-c
16 KW at 3 ϕ , 120/208 volts, a-c

b. Circulation pumps (estimated value)

17.5 at 3 ϕ , 120/208 volts, a-c

c. Generator excitation (estimated value)

6.8 KW at generated voltage

d. Communications (estimated values)

15 KW at 10 kilovolts, d-c
15 KW at 5 kilovolts, d-c
2 KW at 28 volts, d-c

e. Computers and controls (estimated values)

3 KW at 28 volts, d-c
5 KW at generated voltage, a-c (transformed
and rectified internally)

f. Fuel regeneration

90 KW at 105 volts, d-c
15 KW at 3 ϕ , 120/208 volts, a-c (pumps
and compressors)

g. Lunar soil processing

150 KW at generated voltage, a-c

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

The a-c 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 d-c power at about 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 powerplants 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.

3.1.3 Mission Loads Evaluation

The loads discussed in Section 3.1.2 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 generating 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 a-c and d-c grouping results in the following:

1. Interplanetary

a. Unmanned scientific

1420 KW kilovolt, d-c
229 KW low voltage, d-c
76 KW low voltage, low frequency a-c
10 KW generated voltage and frequency
(rectified internally)

b. Manned interplanetary

7654 KW kilovolt, d-c
1720 KW low voltage, d-c
522 KW low voltage, low frequency, a-c
105 KW generated voltage and frequency
(rectified internally)

2. Manned Space Stations

a. Low orbit

221 KW kilovolt, d-c
260 KW low voltage, d-c
20 KW generated voltage and frequency
(rectified internally)
45 KW at low voltage and/or low frequency, a-c
(partially rectified internally)

b. Synchronous orbit

135 KW kilovolt, d-c
514 KW low voltage, d-c
64 KW generated voltage and frequency
(rectified internally)
64 KW at low voltage and/or low frequency, a-c

3. Lunar Base

30 KW kilovolt, d-c
104 KW low voltage, d-c
48.5 KW at low voltage and/or low frequency, a-c
11.8 KW at generated voltage a-c (rectified)
150 KW at generated voltage a-c

The kilovolt d-c loads range between 1 and 30 kilovolts. Those loads requiring low voltage and low frequency a-c 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 cps. Many of these loads, if re-designed, 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 d-c and that much of the a-c 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>	<u>Used as</u>
	<u>Kilovolt</u>	<u>Low Voltage</u>	<u>Internally</u> <u>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 a-c 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 d-c.

It can be seen that for most missions, the d-c loads represent over 80 percent of the total. For all but the lunar base, the relative amounts of low voltage d-c and high-voltage d-c 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 d-c 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.

3.1.4 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(23) 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 cycles per second.

The selection of input voltage to the electromagnetic 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 100-200 volts. However, it appears likely that the design of the unit to operate from the generated or transmitted a-c 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 most 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. Frequencies may cover a range of, for example, 200-2000 cps 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 75-85 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 a-c 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(16) 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, transformer & 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 1 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 d-c voltage (in the order of 100 volts) while for the ion thrusters it will be at d-c voltages in the order of 1-10 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 d-c voltage (20-100 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. For example, on a typical engine 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 a 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 kv.

For many engine designs, heater loads could be supplied as a-c. The SERT program(24) engine designs have utilized the following voltages:

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

Arc jets require voltages of 100-300 volts and although designs have been primarily d-c, the use of a-c may prove desirable. For a-c 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 cps. The arc jets provide lower specific impulses than the ion thrusters ($I_{sp} = 700$ 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 a-c rather than d-c. 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 a-c may eliminate the additional weight and losses of a 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 d-c 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 a-c input. Although the amount of apparatus to condition the generated a-c to usable d-c values is essentially the same whether done internally or externally, the required d-c values can greatly affect the system 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 d-c voltages and is often based on a 28 volt d-c input as standard. Of course, these voltages may also be obtained from an a-c 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 d-c 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 d-c power. As discussed previously, a typical electrolysis cell requires 1.6 volts, 280 amperes, d-c. 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 d-c) where the rectifier supply would have a high efficiency. However, to improve reliability, a series-parallel

matrix arrangement may be desired whereby d-c 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 d-c and 120/208 volts, 400 cps, 3 Ø a-c. Most of the a-c 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 1 percent of the generated power at the high ratings to about 2 percent of the power at the lower system capacities.

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

3.1.5 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, while 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 a-c 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:

- a. Generated a-c
- b. High-voltage a-c
- c. High-voltage d-c
- d. High-voltage a-c and high-voltage d-c
- e. Generated a-c and high-voltage d-c
- f. Low voltage d-c 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 d-c loads there is also a choice on the location of the rectifier when transmitting at the load voltage. Transmission may be a-c with rectification at the load or it may be as d-c 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 cps aircraft a-c systems. Since fast-recovery diodes are required to meet the minimum considered frequency of 400 cps, 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 a-c 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 cyclo-converter 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 system. 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 volt, 400 cps aircraft systems as follows:

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

The above values are based on experience with system 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 d-c bus on aircraft, the proposed MIL-STD-704 revision permits transients that are relatively greater than for the a-c bus. Specified values are:

- a. For 0.85 per unit load with 90 percent interrupted, the maximum voltage is 60 volts for 0.015 seconds (214 percent).
- b. For 1.7 per unit load with 90 percent interrupted, the maximum voltage is 70 volts for 0.02 seconds (250 percent).
- c. 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 (25) 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 a-c bus loads are either less susceptible to damage from transient voltages or are easily filtered for protection. Therefore, the d-c loads are the most critical in the area of transient-voltage induced failures. There is usually a tradeoff 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 cps frequency transient for fault switching in 400 cps 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 noninterruption of loads is critical, a high value of overload would be necessary.

3.1.6 Plans for Coming Quarter

This task is essentially complete with the writing of the summary included in this report. Further work to combine the data with the component parametric data will be done as part of the Electrical Systems Definition task which precedes the Conceptual Design Study.

3.2 Nuclear Mechanical Systems

3.2.1 System Specific Weights

The relationships between the electrical system components and the power plant in which they are utilized are an important part of the basic component design. The Rankine cycle configuration being considered as the power plant is the basic three-loop configuration, Figure 3.2-1, consisting of a reactor loop, a power conversion loop and a heat rejection loop. In these loops, the components of particular interest to the electrical power system may be broadly classified as either "high-temperature" components or "low-temperature" components which consist of the following:

a. High-Temperature Components

- Turbogenerator and accompanying boiler,
condenser and heat exchangers
- Transformers
- Pumps/Motors

b. Low-Temperature Components

- Frequency Changers
- Rectifiers
- Contactors
- Generator Field Control
- Electrical Control Circuits

The electrical transmission lines are classified as both low and high temperature depending on their locations. Payload components are not considered part of the powerplant. Reactors, shields and such are of interest, but not directly so far as the electrical system is concerned.

In order to evaluate and optimize each of the above components relative to the others, at least one common design parameter must be chosen as a basis for comparison. For space power systems, the one most important parameter 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.

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

Figure 3.2-1
Basic Rankine Cycle Configuration

A part of the present study has been to determine the approximate weights of the various powerplant components. This is being 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 rating.

At present, the tabulation of the individual component weights is continuing. Those weights which have been determined are listed in Table 3.2-1 for the four basic ratings at 500, 800, and 1100°F temperature. Complete details of how the various weights were determined will be added when the table has been completed and will be reported in later reports. A few words of explanation are in order however.

The auxiliary radiator cooling system does not show a separate pump. The pump to circulate the auxiliary coolant flow is an integral part of the primary radiator (NaK) pump. The secondary radiator system contains MIPB fluid and operates at an average temperature of 175°F, not the 500 to 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 being listed as if for a single system without redundancy; parallel systems will be analyzed as part of the Electrical Systems Definition Task.

At the bottom of the table, a list of estimated specific weights is shown. These specific weights were estimated on the basis of data presented in the previously indicated references and were made pending the complete analysis of the powerplant weights. This was done in order for system weight penalties to be considered in the initial generator (and motor) parametric screening studies. The specific weights for the heat rejection system (S_L) are for the radiator matrix weight only. The final weights will be much heavier due to structures and plumbing. However, since the data is used on a relative comparison basis, the general conclusions drawn from the first cycle screening studies (Sections 2.1 and 2.2) should remain valid for the heavier weights.

3.2.2 Effective System Weights

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. For

*References 16, 17, 20, 21, 28, 29, 30, 31, 32 and 33.

Table 3.2-1 Powerplant Component Weights

	Rating Temp. °F	0.35 MW _e			1.0 MW _e			5.0 MW _e			10.0 MW _e		
		500	800	1100	500	800	1100	500	800	1100	500	800	1100
Component	Units												
1. Reactor - 10,000 hr. life	lbs		*			*			*			*	
40,000 hr. life	lbs		*			*			*			*	
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		*			*			*			*	
7. Reactor Coolant Pump	lbs		*			*			*			*	
8. Primary Radiator	lbs	← 1500 →			← 4291 →			← 21450 →			← 42910 →		
9. Turbine	lbs		*			*			*			*	
Subtotal - 10,000 hr.	lbs												
- 40,000 hr.	lbs												
Specific Weight (S _g) -10,000 hr	lb/kw												
-40,000 hr	lb/kw												
10. Auxiliary Radiator	lbs	167	57	24	391	132	57	1710	579	247	5480	1180	504
11. Auxiliary Radiator Heat Exchanger	lbs		*			*			*			*	
Specific Weight (S _L)	lb/kw												
12. Secondary Radiator	lbs	← 378 →			← 953 →			← 6600 →			← 12600 →		
13. Secondary Coolant Pump	lbs	← 13 →			← 16 →			← 34 →			← 46 →		
Specific Weight (S _L ')	lb/kw	← 44 →			← 44 →			← 51 →			← 51 →		
Temporarily Assumed Specific Weights**													
Estimated S _g		← 17 →			← 15 →			← 13 →			← 12 →		
Estimated S _L		3 1.3 0.7			3 1.3 0.7			3 1.3 0.7			3 1.3 0.7		
*Data Pending completion.													
**Estimated for use in initial parametric screening studies, to be updated when above table is completed.													

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}

$$W_{ge} \equiv W_g + S_g (KW_g) + S_L (KW_{gL})$$

W_g = actual generator weight = electrical weight
plus structures weight

S_g = specific weight of system supplying shaft power.

$$\begin{aligned} KW_g &= \text{input power to generator} = (KVA * P.f.) + KW_{gL} \\ &= KW_e + KW_{gL} \end{aligned}$$

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,

$$EFF_g = \frac{KW_e}{KW_e + KW_{gL}} \text{ or } KW_{gL} = \frac{KW_e}{EFF_g} - KW_e$$

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

$$W_{ge} = W_g + KW_e \left(\frac{S_g + S_L}{EFF_g} - S_L \right)$$

The effective system weight penalty is simply

$$\text{Gen. Wt. penalty} = (S_g + S_L) KW_{gL} = (S_g + S_L) (KW_e) \left(\frac{1}{EFF_g} - 1 \right)$$

B. Effective Motor System Weight, W_{me}

$$W_{me} = W_{pm} + S_e' KW_p + S_L KW_{pL}$$

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

$$= (W_{BFP} + W_{pRp} + W_{RCP}) \div KW_e$$

W_{BFP} = weight boiler feed pump/motor

W_{pRp} = weight primary radiator 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,

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

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

$$EFF_{hp} = \frac{KW_{rating}}{KW_{rating} + KW_{hpL}} \quad \text{or} \quad KW_{hpL} = KW_{rating} \left(\frac{1}{EFF_{hp}} - 1 \right),$$

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,

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

3.2.3 Rotor Inertia and Transient Response

Another important part of this task is to examine pertinent relationships between the various electrical components and the whole power system. One of the relationships recently examined warrants 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 the 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,

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

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

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

$$w_0 = \text{shaft speed, radians per second.}$$

Also, by definition,

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

Thus,

$$t = \frac{I w_0^2 (\Omega) (0.746)}{\text{kwe} 6600} = \frac{I w_0^2}{2} \left(\frac{B_I \Omega (0.746)}{\text{kwe} (3300)} \right) (\text{seconds})$$

$$\text{Where} \quad B_I = 1 + \frac{I_t}{I_g}$$

$$I_t = \text{inertia of turbine, in lb sec}^2$$

$$I_g = \text{inertia of generator, in lb sec}^2$$

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

A check of the most promising generator designs in the first cycle parametric study output 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 3.2-2. 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 3.2-3.

The resulting times are considerable and should allow ample freedom in designing the speed control system to prevent transients from exceeding five percent. At 10 percent overspeed, the times would be double those shown.

3.2.4 Plans for Coming Quarter

The specific weights of the Rankine configuration selected will be completed, tabulated and identified as to their source.

The requirements and limitations imposed on the electrical components by the prime mover system will be summarized relative to turbine-generator integration, system pumping requirements, low temperature cooling limitations and weight and volume limitations.

Figure 3.2-2

Rotor Kinetic Energy Versus Generation Electrical Rating

Figure 3.2-3

Time Required to Accelerate Rotor to 5 Percent Overspeed Versus Generator Electrical Rating

4. EXPERIMENTAL VERIFICATION

4.1 Magnetic Force Unbalance and Flux Distribution

4.1.1 Magnetic Force Unbalance

A. Background and Test Objectives - In the normal selection or design of a bearing system, bearing load is an important parameter. The weight of the rotating member is an obvious load for the bearings when operating in a gravity application. In electromagnetic generators, a magnetic radial force or axial moment can result if the rotating member is made eccentric to or misaligned with the stator. (Misalignment refers to tilting of the rotor axis with respect to the axial centerline of the stator). The magnitude of the radial force or axial moment is dependent upon the degree of eccentricity and/or misalignment between the rotor and stator and the level of field excitation. The magnetic force on the rotor will be present regardless of the conditions of gravity and can be a significant portion of the total bearing load.

The test objective is to measure directly the net forces acting on the rotor of the 467 kva Experimental Generator(3) for various degrees of eccentricity, misalignment, and field current and to measure the unbalanced magnetic fields associated with these forces. The magnetic fields exist in the radial gaps of the generator. When the rotor is moved eccentrically to the stator bore, these fields become unbalanced and create a force on the rotor which acts in the same direction of the rotor displacement with respect to the stator.

B. Test Description - Search coils were wound around the poles of the 467 kva Experimental Generator rotor near the faces of the poles as shown in Figure 4.1-1. The rotor is mounted to the stator through anti-friction bearings, load cells, and adjustable linkage, while the stator in turn is mounted in a fixture as shown in Figure 4.1-2.

Anti-friction bearings are provided to facilitate the use of dial indicators which were mounted on the rotor to measure eccentricity and misalignment. The load cells are provided to measure the magnetic force imposed on the rotor. Adjustable linkage is provided to accurately adjust the eccentricity and/or misalignment of the rotor relative to the stator bore. All mounting fixtures and adapters were constructed of non-magnetic stainless steel to avoid any distortion of the generator flux distribution.

Figure 4.1-1
Rotor with Search Coils and Bearings

Figure 4.1-2
Stator Test Fixture

The magnetic field unbalance associated with a given rotor eccentricity was measured by connecting flux meters to the rotor search coils. The search coils wound around a diametrically opposite pair of poles were connected in series with the coil polarities such that the readings would be additive. After a set of readings were obtained for a given rotor eccentricity, the coils were reconnected with the coil polarities such that the readings would be subtractive and the test repeated. The net magnetic force acting on a pair of diametrically opposite poles is directly proportional to the difference of the squares of the flux quantity emanating from or entering the pole surfaces.

The net force acting on a diametrically opposite pair of poles is determined from the product of the flux measurements taken as described above since $(\phi_2 + \phi_1)(\phi_2 - \phi_1) = \phi_2^2 - \phi_1^2$. Dial indicators were mounted to the test fixture frame in order to measure the radial movement of the rotor as a result of load cell deflection under load. This deflection must be included to determine total rotor eccentricity under load.

Field current was increased in three ampere increments to 30 amperes. Fluxmeter, load cell, dial indicator, field voltage and field current readings were taken at each increment of field current. After each reading, the fluxmeters were reset to zero and held until just prior to raising the current to the next incremental level. The meter readings were added to obtain the total flux curve as a function of field current. This procedure was used to minimize errors caused by drifting of the meter pointers. The stator and rotor were demagnetized after each test run by reversing the polarity of the field current while decreasing the field current from 30 amperes to zero. Tests were run for the following values of rotor eccentricity and misalignment.

<u>Eccentricity</u> <u>Inches</u>	<u>Misalignment</u> <u>Inches/Inch</u>
0	0
0.005	0
0.010	0
0.015	0
0.020	0
0.030	0
0	0.0002
0	0.0005
0	0.0010
0.005	0.0002
0.005	0.0005
0.010	0.0005

The load cells, load indicating meter, and fluxmeters were calibrated prior to performing the tests and checked at frequent intervals during the test.

C. Calculation of Radial Force from Magnetic Field Test Data -
When a magnetic field, B, exists in the space between two magnetized surfaces, there exists a force equal to

$$F = \frac{B^2 A}{72.13} \text{ lbs.} \quad (4.1-1)$$

B = flux density in kilolines per square inch
A = cross sectional area of the gap in square inches.

Since the flux density, B, is equal to the uniform flux, ϕ , enclosed by the area, A, divided by that area, equation 4.1-1 can be written as follows:

$$F = \frac{\phi^2}{72.13 (A)} \text{ lbs.} \quad (4.1-2)$$

The amount of energy per unit volume (i.e., energy density) in a given space is identical with pressure or force per unit surface area when the surface is taken at right angles to the direction in which the pressure can act to move a substance. Whenever there is an immediately available supply of energy, a mechanical force is in evidence. The energy stored in a magnetic field is

$$E = \frac{(B)^2}{72.13 (Mr)} \frac{\text{inch-lb}}{(\text{inch})^3} \quad (4.1-3)$$

Mr = relative permeability

For a field density of 8.49 KL/inch² in space where Mr = 1, there exists an energy density of 1.0 inch-lb/(inch)³ or 1.0 lbs/(inch)². Hence the pressure tending to move the two bodies together will be equal to 1.0 pounds for each square inch of cross section of the gap. If the cross section is 10 square inches, the mechanical force of mutual attraction will be 10 pounds. Therefore, the force equals the energy density, E, times the cross sectional area, A. In air or free space where the relative permeability, Mr, is unity, the product of E times A results in equation 4.1-1.

Referring to Figure 4.1-3 which is an end view of a 16 pole inductor alternator rotor (8 poles are located on each end of the rotor), a magnetic force, F, is shown perpendicular to each pole face, acting to pull the rotor towards the stator bore surface. There is no net magnetic force acting on the rotor if these forces are all equal. For this condition to exist, the magnetic field, B, at the surface of each pole must be equal. This occurs only when the rotor is perfectly concentric with the stator. When the rotor

Figure 4.1-3
End View of 16-Pole Rotor

is moved eccentrically to the stator as shown in Figure 4.1-4, the reluctance (equivalent to resistance in an electrical circuit) of the magnetic path for the bottom poles is less than that for the top poles due to the shorter length of radial pole gaps. Hence, the flux density and corresponding force at the surface of the bottom poles is greater than for the top poles, resulting in a net magnetic force acting downward in the direction of the eccentricity. As a matter of interest pertinent to this discussion, a 2-pole rotor will be examined. Figure 4.1-5 shows a 2-pole rotor positioned eccentrically to the stator. In this case, the flux path is common to each of the two poles and therefore the flux density and corresponding force is the same for each pole. There-

fore, no net radial magnetic force acts on a 2-pole rotor regardless of the condition of eccentricity. A large axial moment does exist, however, which makes a 2-pole inductor generator impractical. We see then that the magnitude of the net radial force on an eccentric rotor depends on the number of rotor poles.

Figure 4.1-6 shows a stretched out view of one end of an inductor generator rotor with the corresponding flux density distribution in the radial gaps shown above. The density is a maximum over the pole faces and decreases to a minimum over the center of the inter-polar space. The distribution at the other end of the rotor is identical except it is in the opposite direction and shifted 90 electrical degrees.

The magnitude of the force of attraction is proportional to the square of the flux density at all points in the radial gap. The flux density over the pole faces is normally greater than 10 times the density at the center of the inter-polar space and therefore the magnitude of the magnetic force over the pole faces is normally greater than 100 times the force over the center of the inter-polar space. For this reason the vicinity of the pole face becomes the prime area for investigation of magnetic force unbalance. The contribution of the inter-polar space away from the pole to the net radial force on the rotor is small.

Figure 4.1-4
Rotor Eccentric to Stator

Figure 4.1-5
Two Pole Rotor

Figure 4.1-6
Rotor Poles and Flux Distribution

In order to include the total flux, it would require that the flux density distribution be determined for at least half of the stator bore periphery. An integration of the flux density distribution according to equation 4.1-1 would then yield the force distribution. The flux distribution could be obtained with a Hall Effect Gaussmeter, but it would be a very difficult and tedious process.

It was concluded that the most accurate and efficient method for determining the magnetic field unbalance would be to measure the flux emanating from the rotor poles with search coils wound around the pole bodies near the tips of the poles.

A plot of per unit radial gap specific permeance for one pole pitch is shown in Figure 4.1-7. The axis of abscissas is dimensioned in electrical degrees, there being 180 electrical degrees per pole pitch. The plot also represents the flux density distribution in the gap since flux density is directly proportional to permeance. The distribution corresponds to that of the test rig with the rotor set concentrically to the stator (the radial pole gap = 0.122 inch) and with the stator teeth and rotor poles unsaturated. The axis of ordinates coincides with the centerline of a rotor pole. One half pole width corresponds to 60 electrical degrees for the test rig. The flux density does not drop immediately after the edge of the pole face is reached but instead it decays somewhat exponentially to the value at the center of the inter-polar space (180° point).

Figure 4.1-7
Radial Gap Per Unit Specific Permeance

This is due to the fact that the flux in the vicinity of the pole face edge bends to intercept the side of the pole and therefore the search coils wound around the pole bodies will measure an amount of flux greater than that emanating from the pole faces.

The flux intercepting the sides of the poles must be considered to determine the total flux linking the search coils. The flux distribution around the edge of a rotor pole for a radial pole gap of 0.122 inch is shown in Figure 4.1-8. Ten conductors of a search coil are shown located against the pole body. The coils extend about 0.2 inch down the sides of the pole. In this plot, the fringing flux produces 33 flux linkages in the search coil. It would be equivalent to consider that the fringing flux entering the top one third of the search coil height links all ten-turns of the coil. A good approximation of the total flux linking the search coil can be made by extending the half width of the search coil by a distance equivalent to the normal radial pole gap (0.122 inch) plus one-third of the coil height (0.66 inch). This distance corresponds to 15 electrical degrees along the pole pitch. Referring to Figure 4.1-7, the total flux linked by a rotor pole search coil would correspond to the area under the curve up to the 75 degree point on the axis of abscissas.

It would be convenient to determine a correction factor for equation 4.1-2 which would account for the non-uniform distribution of the flux and for the effective increase in pole face area due to the fringing flux. This was performed in the following manner. A plot of radial gap specific permeance for a pole gap of 0.092 inch and 0.152 inch respectively is shown in Figure 4.1-9. This plot approximates the conditions for the top and bottom poles of the test rig for a rotor eccentricity of 30 mils. As stated previously, the flux density distribution is identical to the permeance distribution in the radial gap. Since force is proportional to the square of the flux density at each point in the radial gap, the net force distribution can be found for the diametrically opposite poles by plotting the difference of the squares for each point on the curves. The net force distribution is shown in Figure 4.1-10. The area under this curve is proportional to the total net force on the pair of poles. The search coils measure a quantity of flux represented by the area under the curves of Figure 4.1-9 up to the 75 degree point on the axis of abscissas. The ordinates which give equivalent areas for a half pole width (60 degrees) were found. The difference of the squares of these two ordinates multiplied by 60 degrees is proportional to the force that would be calculated from equation 4.1-2 using the actual pole face area. The ratio of the area under the force distribution curve of Figure 4.1-9 to the area calculated from the difference of the squares of the ordinates as previously described, gives the proper correction factor. For a rotor eccentricity of 30 mils, the correction factor is 0.834. This procedure was repeated for a rotor eccentricity of 5 mils. (Reference Figures 4.1-11 and 4.1-12) and resulted in a correction factor of 0.820. The correction factors are nearly equal for both the largest and smallest rotor eccentricities tested and therefore a mean value of 0.83 was

Figure 4. 1-8
Flux Distribution

Figure 4.1-9

Specific Permeance, 30 Mil Eccentricity

Figure 4. 1-10

Force Distribution, 30 Mil Eccentricity

Figure 4.1-11

Specific Permeance, 5 Mil Eccentricity

Figure 4. 1-12

Force Distribution, 5 Mil Eccentricity

assumed to hold for all conditions of rotor eccentricity and misalignment. Figure 4.1-13 shows the 16-pole test rotor positioned eccentrically to the stator bore by a distance "e". A corresponding force diagram with concentrated forces acting at the center lines of the poles is also shown. Poles 1 and 9, 2 and 10, etc., are 180 degrees apart and therefore the forces acting on these poles may be subtracted directly to obtain the force diagram shown in Figure 4.1-14.

Figure 4.1-13
Eccentric Rotor and Force Diagram

Figure 4.1-14
Force Diagram

The horizontal components of the composite forces cancel out due to symmetry leaving a net force which equals the sum of the vertical components. This force acts in the direction that the rotor was moved with respect to the stator (i.e., in the direction of the eccentricity).

The search coils wound around poles 1 and 9, 2 and 10, 3 and 11, etc. were connected in series with the coil polarities such that the readings would be additive.

After a set of flux readings were obtained for a given rotor eccentricity, the coils were reconnected with the coil polarities

Figure 4.1-15

Radial Magnetic Force on Rotor

such that the readings would be subtractive and the tests repeated. For poles 1 and 9, for example, we find from equation 4.1-2 and the correction factor that

$$F_9 - F_1 = \frac{0.83 [(\phi_9)^2 - (\phi_1)^2]}{72.13(A)} \quad (4.1-4)$$

$$= \frac{0.83 (\phi_9 + \phi_1) (\phi_9 - \phi_1)}{72.13(A)} \text{ lbs}$$

ϕ = flux in kilolines

where A is the area of the pole face. When this is done for each of the seven diametrically opposite pole pairs, the vector diagram of Figure 4.1-14 is obtained. The vertical components of this diagram gives the net radial force acting on the rotor for the particular conditions of test.

D. Test Results - Figure 4.1-15 shows a comparison of the total magnetic force acting on the rotor as measured by the load cells and as calculated from the flux measurements for various values of field current and rotor eccentricity. The force increases exponentially with field current up to about 15 amperes due to the practically linear relationship between field current and flux for low values of field excitation. Above 15 amperes field current, the effects of magnetic steel saturation begin to appear. That is, the rate of change of flux per ampere increase in field current starts to decrease. These effects can be deduced from the typical generator saturation curve shown in Figure 4.1-16. The force on the rotor reaches a peak at about 21 amperes field current and then starts to decrease. This decrease in force is caused by high magnetic saturation of the rotor poles and the stator teeth immediately above these poles. That is, the flux from all the poles is approaching the saturation limit of the steel. The magnitude of the flux at each pole must be different to cause a net force on the rotor, and since the flux for each pole is approaching a common limit, the net force on the rotor decreases as the field current is increased.

Figure 4.1-16
Typical Generator Saturation
Curve

Of most interest to the designer is the maximum magnetic force imposed on the rotor for a given rotor eccentricity. Figure 4.1-17 shows the maximum force on the rotor as determined by test, plotted as a function of rotor eccentricity. The forces were

Figure 4.1-17
Maximum Radial Magnetic Force on Rotor

taken from the load cell readings and the eccentricity is comprised of the initial rotor displacement and the additional displacement caused by load cell deflection. The plot, as shown, is a straight line with a slope of 19.114 pounds per 0.001 inch eccentricity. Maximum force occurs at 21 amperes field current. Thus, for the 467 kva Experimental Generator, which has a maximum rotor eccentricity tolerance of 0.006 inch, a magnetic force of up to 115 pounds on the rotor can be expected.

Analysis of test results has not progressed sufficiently at the time of writing to present the effects from rotor misalignment. This condition, which is an angular displacement of the rotor axis of rotation from the centerline of the stator bore, theoretically produces a force moment on the rotor along with the axis of rotation. Preliminary results indicate that the resulting moment on the rotor is small for the range of misalignments investigated.

4.1.2 Magnetic Flux Distribution Tests

A. Background and Test Objective - The generator output voltage at any given speed is a function of the magnitude of the change in magnetic flux cutting the stator conductors. This flux can therefore be considered the useful flux, while the difference between this useful flux and the total flux is considered leakage flux. An important step in the initial design of any generator is to compute the values of both useful flux and leakage flux. It is necessary to experimentally verify these flux distribution calculations so that: (1) accurate predictions of generator performance and operating characteristics can be made well in advance of development testing, and (2) pertinent flux leakage data can be established for large inductor generators suitable for nuclear space electric power systems. Determination of this data now will improve the design accuracy of later generator configurations required by such programs.

B. Test Description - The essential requirement of these tests are to measure the distribution of the flux in the stator and rotor components of the 467 kva Experimental Generator. To accomplish this the stator was fitted with seven search coils and the rotor with 13 search coils, seven of which were required for the magnetic force tests. Field excitation was provided by means of a 100 volt, 100 ampere d-c power supply. Field current was increased in three ampere increments to 30 amperes. Fluxmeters and field current readings were taken at each increment of field current. After each reading, the fluxmeters were reset to zero and held until just prior to raising the current to the next incremental level. The meter readings were added to obtain the total flux curve as a function of field current. This procedure was used to minimize errors caused by drifting of the pointers. The test rig was demagnetized after each test run by reversing the polarity of the field current from 30 amperes to zero.

C. Test Results - Analysis of test results has not progressed at the time of writing, to the point where a full discussion of test results can be presented. Preliminary results indicate that the main path flux (i.e., the flux in the radial gap that cuts the a-c windings) is from 5 percent to 10 percent higher than predicted for a given field current. Of greater significance is the discovery that the a-c flux in the DBS (depth behind slot) of the armature laminations is about one third of the calculated value. Assuming iron loss is proportional to the 1.6 power of flux density, the DBS loss may be as low as 17 percent of the previously calculated value. The calculated DBS loss represents about 37 percent of the total generator loss under load which results in a net gain of about three percentage points in efficiency.

4.2 Magnetic No-Load Loss Tests

4.2.1 Past Quarter Accomplishment

Figure 4.2-1 illustrates the final configuration of the No-Load Loss Test assembly configuration. Slight modifications were made to the previously reported configuration in order to isolate the bearings magnetically and hence reduce the possibility of arcing and pitting in the bearings.

All detail drawings were completed and released for manufacture. All raw materials were in stores or were ordered and have been received. All purchased components have been ordered. Approximately half of these components, including the bearings, were received. Approximately 25 percent of the manufactured components were completed.

The rotor was removed from the static test assembly and the search coils were removed. First alterations to the rotor flow circuit were completed by soldering three plugs into rotor flow exit holes previously drilled for the liquid metal cooled configuration. The rotor is presently being machined to blend all sharp corners which would act as stress risers during subsequent rotation on test and to slot the pole faces. These fabrication operations will not alter the magnetic configuration of the rotor.

4.2.2 Plans for Next Quarter

The No-Load Loss Test Assembly will be completed and all testing is scheduled to be completed. Evaluation of the test data and preparation of the test report will be initiated.

Figure 4.2-1
No-Load Dynamic Test Rig

4.3 Rotor Cooling Concepts

4.3.1 Past Quarter Accomplishments

The test fixture which was used to verify the fluid dynamic performance of the rotor assembly and related components for the 467 kva Experimental Generator⁽³⁾ have been modified to accommodate a new rotor assembly. The new rotor assembly has a coolant flow path which diverts the coolant radially outward from the center to a diameter of 7.0 inches. The coolant is then diverted radially inward to a diameter of 2.0 inches. A rotor with this type of flow passage provides efficient cooling of the rotor since the heat conduction path between the pole face and coolant is reduced. The primary objective of the experimental work to be performed with the new rotor assembly is to verify the flow capacity and stability of this rotor coolant flow concept. The rotor and related components to be used for these experiments are shown in Figures 4.3-1 and 4.3-2. These parts have since been assembled. A dimensional analysis which included measuring and recording all critical dimensions, eccentricities, and squareness of components was made as the parts were assembled to verify that the running clearances were adequate. Figure 4.3-2 is a photograph of the turntable and face plate used to make these measurements.

The test fixture has been placed on a test stand as shown in Figure 4.3-4 and a check out of the equipment and instrument was initiated on February 13, 1967

Testing is in progress at the time of this writing.

4.3.2 Plans for Coming Quarter

The tests shall be completed by the end of the next reporting period. Preparation of the test report will be initiated.

Figure 4.3-1
Fluid Dynamic Test Rig

Figure 4.3-2
Antidrive-end View

Figure 4.3-3
Drive-end View

REFERENCES

1. 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.

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.
2. "Screw Type Shaft Seals for Potassium Lubricated Generators" by A. E. King, Supplement to IEEE Transactions on Aerospace, Vol. AS-3, No. 2, June, 1965.
3. Work on the SNAP-50/SPUR Generator covered a period from 1960 to 1966 and was carried out under U.S.A.F. Contracts AF33(616)-7379 and -8322, AF33(657)-8954 and -10922, and AF33(615)-1551 and -2326. Final reports on this program are presently being published.
4. Analysis of the Viscoseal, Part II, The Concentric Turbulent Case, NASA Report No. NASA-CR-76866, U. of Tenn., Dept. of Mechanical & Aerospace Engineering, June, 1966.
5. Centrifugal Types of Dynamic Shaft Seals, ASME Paper No. 63-WA-176, November, 1963.
6. SNAP-50/SPUR Frequency Changer Preliminary Design and Specification, Westinghouse Electric Aerospace Electrical Division, U.S.A.F. Report AFAPL-TR-65-98, December 15, 1965.
7. Electrical Engineer's Handbook, Pender & Del Mar, 4th Ed., John Wiley & Son, pp. 11-19.
8. High Power Modular Inverters and Converters Summary Report - Task I, by P. F. Pittman & R. J. Rovas, Westinghouse Electric, Central Research Labs, Research Department 66-8M5-MODIN-R1 (ESR 226).

9. "Power Transmission at High Frequency" by F. P. Emery & Darius Irani, IEEE Transactions on Aerospace, Volume AS-3, No. 2, June 1965.
10. Electro-Magnetics, by John D. Kraus, McGraw-Hill Book Company, Inc., New York (1953).
11. "Inductance and Resistance in Telephone and Other Circuits" by Dr. J. W. Nicholson, Philosophical Magazine, Volume 17.
12. High Voltage Vacuum Contactors, Jennings Radio Mfg. Corp., Catalog No. 104, San Jose, Calif., January, 1966.
13. Development of Electrical Switchgear for Space Nuclear Electrical Systems, Contract NAS 3-2546, General Electric, Missile and Space Division, NASA Report NASA CR 54247, (Phase I Final Report), November, 1964.
14. "Switching in High Vacuum Environment" by Hugh C. Ross, IEEE Transactions on Component Parts, Volume CP-10, No. 4, December, 1963.
15. Development of Electrical Switchgear for Space Nuclear Electrical Systems, Contract NAS 3-6467, General Electric, Missile and Space Division.

3rd Quarterly	Report No. NASA CR54743 10/65
4th Quarterly	Report No. NASA CR54861 1/66
5th Quarterly	Report No. NASA CR54924 4/66
6th Quarterly	Report No. NASA CR72014 7/66
16. 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
17. 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.

18. Manned Orbital Space Station Power Conversion and Control, Report No. SID-63-36-6, Westinghouse Electric, Aerospace Electrical Division, February 1, 1963.
19. Study for an On-Board Electrical Power System for a Manned Orbital Space Station, Contract NAS 9-1307, Lockheed Aircraft Co., November 24, 1963.
20. Mission and Power Subsystems Studies, Vol. II, General Electric, Missile and Space Division, Report No. AFAPL-TR-65-56, July, 1965.
21. 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.
22. Engineering Study of Multi-Purpose Engine and Fuel System, Lunar Exploration Systems 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.
23. Electromagnetic Alkali Metal Pump Research Program, Contract NAS 3-2543, General Electric Co., Report No. NASA CR-380, February, 1966
24. "Power Conditioning Requirements for Ion Rockets" by J. T. Kotnik and B. L. Sater; IEEE Transactions on Aerospace, Vol. 2, No. 2, April, 1964.
25. 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.
26. Design Study, Electrical Component Technology for 0.25 to to 10.0 Megawatt Space Power Systems, 1st Quarterly Report, AEC Contract AT(04-3)-679, Westinghouse Electric, Aerospace Electrical Division, AEC R&D Report SAN-679-1, November, 1966.
27. Summary Report, SNAP-50/SPUR Pump Motor Development Program, Pratt & Whitney Aircraft Division - CANEL Report No. 470, PWAC Contract AT(30-1)2789, prepared by Westinghouse Aerospace Electrical Division under agreement 65-9, September 3, 1965.
28. Advanced Nuclear Electric Power Generator System Study, Rankine Cycle Nuclear Spare Powerplant, Pratt and Whitney Aircraft Co., Report No. PWA-2232, August, 1963.

29. SNAP-50 Optimization Studies, Pratt & Whitney Aircraft Report PWAC-391, March 1, 1963.
30. SNAP-50/SPUR First Flight Power Plant Preliminary Design Specification, P & W AC Report CNLM-5118, June 21, 1963.
31. SNAP-50/SPUR Powerplant Specifications, P & W AC Report CNLM-4400, Supplement 6, June 1, 1964.
32. SNAP-50/SPUR Powerplant Requirements Report, P & W AC Reports CNLM-6291 through 6297, June, 1965.
33. Space Powerplant Study, Contract NAS 5-250, Westinghouse Electric, Astronuclear Labs, Report WANL-PR-(B)-009, December, 1963.