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DETAILED DESIGN, FABRICATION AND TESTING
OF AN ENGINEERING PROTOTYPE
COMPENSATED PULSED ALTERNATOR

Lawrence Livermore Laboratory
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Prepared by

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ABSTRACT

The design, fabrication, and test results of a prototype compensated pulsed alternator are discussed. The prototype compulsator is a vertical shaft single phase alternator with a rotating armature and salient pole stator. The machine is designed for low rep rate pulsed duty and is sized to drive a modified 10 cm Beta amplifier. The load consists of sixteen 15 mm x 20 mm x 112 cm long xenon flashlamps connected in parallel. The prototype compulsator generates an open circuit voltage of 6 kV, 180 Gertz, at a maximum design speed of 5400 rpm. At maximum speed, the inertial energy stored in the compulsator rotor is 3.4 megajoules.

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1. COMPENSATED PULSED ALTERNATOR PROGRAM

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1. THE COMPENSATED PULSED ALTERNATOR PROGRAM

Introduction

The Center for Electromechanics of the University of Texas at Austin has proposed an alternative pulsed power supply to drive xenon flashlamps for pumping solid state laser amplifiers for NOVA. The proposed power supply is the compensated pulsed alternator, or Compulsator. The primary incentive for pursuing this program is to develop a more compact and less expensive energy storage and conversion system alternative to conventional capacitor banks in the ten to hundred megajoule class.

The primary energy store of the system is the spinning alternator rotor. Therefore, the high energy density and low cost of inertial energy storage is basic to the design. The fundamental development problem is to improve the pulse power rating of the machine by an order of magnitude over conventional short circuit generators. Our approach to the solution of this problem is based on the following design features:

1. High speed laminated rotor assembly.
2. Air gap armature and compensating windings
3. High shear strength electrical insulation system.
4. High average magnetic flux density over polar pitch.

The pulse power rating is increased by reducing the armature circuit reactance. This is accomplished in three ways. First, a compensating winding is connected in series opposition to the armature winding to confine the armature flux at peak current to the air gap between the windings. Second, the armature winding and compensating winding are bonded to smooth steel surfaces of the rotor and stator, respectively. The conductors are not imbedded in slots, thereby eliminating slot leakage reactance. Finally, the conductors are made radially thin so that the windings approach the low inductance form of cylindrical current sheets.

Since the coupling between the armature winding and compensating winding changes with the angular position of the rotor, the effective armature inductance is time variant. The time varying armature circuit inductance controls the shape of the output current pulse and helps to provide

a narrow pulse required for proper flashlamp operation. If the variation in inductance is made significant, a 500 microsecond half width pulse may be generated from a machine with a much lower open circuit base frequency.

The peak power rating is also increased by maximizing armature voltage. This is accomplished by driving the magnetic circuit to peak flux densities of 2.0 Tesla over a large fraction of the pole pitch. This is possible because the rotor periphery and stator bore are smooth cylindrical structures. In conventional rotating machinery, the peak flux density in the teeth may approach 2.0 Tesla, but the average flux density over the pole face may be only 1.0 Tesla.

The penalty for designing the machine in this manner is that the armature circuit conductors are exposed to the full applied field at 180 Hz. Therefore, the full energy conversion forces are developed at the conductor and not on iron teeth. Since the moment of inertia of the conductors is a small fraction of the total inertia of the rotor, this force must be transmitted by the electrical insulation system in shear. In addition, the armature conductors must be finely stranded and transposed to reduce open circuit eddy current losses.

Since the physical configuration of the compensated pulsed alternator and the required manufacturing methods differ substantially from conventional rotating machine technology, the Center for Electromechanics proposed to prove the principle of operation and to develop an engineering design base on a third to half scale prototype. The prototype machine is sized to drive a flashlamp load consisting of sixteen 1.5cm x 112cm xenon filled lamps connected in parallel.

Engineering Prototype Compulsator

The engineering prototype compensated pulsed alternator is a vertical shaft single phase machine with a rotating armature and a salient pole stator. A cut-away drawing of the prototype is shown in Figure 1-1. The compulsator rotor develops an open circuit voltage of 6 kV at the maximum design speed of 5400 rpm. The open circuit frequency of the four pole machine is 180 Hz. At the maximum design speed the inertial energy stored in the rotor is 3.4 megajoules.

Figure 1-1. Cut-away View of Engineering Prototype Comperated Pulsed Alternator

The machine was designed and built by the Center for Electromechanics under Lawrence Livermore Laboratory Purchase Order No. 3325309 issued September 21, 1978. Fabrication and assembly was completed in late September, 1979, and initial testing was terminated December 20, 1979. The total project expenditure was \$554,957.

Summary of Results

The prototype compulsator was discharged successfully into the flash-lamp load a total of 27 times from speeds ranging from 1800 rpm to 4800 rpm. Including mechanical tests the prototype compulsator was motored over 120 times to speeds up to and including the maximum design speed of 5400 rpm.

The machine delivered a maximum of 135 kilojoules to the flashlamp load at a peak current of 30 kiloamps with a pulse width of 1300 microseconds. The test program was terminated after a short circuit occurred at 5400 rpm, 500 milliseconds before the series ignitron and flashlamps were to be triggered. The armature and compensating windings were locally damaged by what appears to be thermal runaway at several hot spots on the winding. At the time of this writing the machine has been disassembled for inspection of damage. The machine is being prepared for static inductance measurements of an improved winding configuration and, later, for reassembly.

The performance of the machine, as originally designed, was primarily limited by the presence of eddy currents in the solid pole faces. These currents reduced the armature inductance and increased armature resistance when the rotor winding was not in the compensated position. The performance of the machine can be improved by reducing the eddy currents by incorporating a laminated stator structure into the design, and by providing a lower resistance path for the eddy currents that flow in the end turn region of the rotor.

The Center for Electromechanics has developed a space harmonic distribution analysis technique for calculating magnetic field strengths to determine armature inductance variation and eddy current losses for a cylindrical geometry. This analytical tool has proved useful in obtaining a good match between theoretical calculations and actual measurements for the engineering prototype. The technique is presently being expanded to investigate other compulsator configurations that may better address the issues raised during the initial test program. The continued research and development effort is presently funded under Lawrence Livermore Subcontract No. 8030909.

2. PROTOTYPE COMPULSATOR PULSE POWER CIRCUIT PERFORMANCE

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2. PROTOTYPE COMPULSATOR PULSE POWER CIRCUIT PERFORMANCE

Description of the Pulse Power Circuit

The prototype compensated pulsed alternator has been designed to drive a prototype Beta amplifier. The 10 cm Shiva Laser amplifier contains 16 Shiva flashlamps connected in parallel. Therefore, the peak system voltage is approximately six kilovolts. Since gain measurements are not being made under the scope of this contract, the laser glass disks have been removed. The 15 by 20 mm 300 Torr xenon flashlamps have an arc length of 112 cm and are triggered by pulsing the voltage of the lamp reflectors relative to the lamp electrodes. The flashlamps then serve as a switch in the circuit so that only one ignitron is required. A simplified schematic diagram of the prototype compulsator test circuit is shown in Fig. 2-1.

Figure 2-1. Simplified Schematic Diagram
of Prototype Compulsator Test Circuit.

The prototype compensated pulsed alternator parameters are listed in Table 2-1.

TABLE 2-1
PROTOTYPE COMPULSATOR PARAMETERS

Number of Poles	4
Rotor Speed (rpm)	5400
Rotor Angular Velocity (sec^{-1})	565
Polar Moment of Inertia (J-sec^2)	22
Rotational Energy Store (MJ)	3.5
Open Circuit Frequency (Hz)	180
Peak Open Circuit Voltage (kV)	5.8
Minimum Armature Circuit Inductance (μH)*	26.3
Maximum Armature Circuit Inductance (μH)*	176
Armature Circuit Resistance at 20°C (DC $\text{m}\Omega$)	54
Field Circuit Voltage (V)	108
Field Circuit Current (A)	1275

* Measured with LCR bridge at 1 kHz

Other components of the pulse power conditioning equipment are listed in Table 2-2.

The 700 μf capacitor bank performs two important functions during the initial startup phase of the discharge sequence. After the lamp reflectors are pulsed to break down the flashlamps, a fraction of the capacitor bank energy is dumped into the lamps to expand the lamp arc to near full bore to achieve a low impedance state. The remaining capacitor bank energy is discharged into the alternator when the series ignitron is fired. The time delay between triggering the flashlamps and triggering the ignitron is variable, since both trigger signals are provided by the eight channel delay generator. The energy discharged into the compulsator helps to establish an initial amature flux in the air gap between the rotor and compensating windings. This flux is then compressed as the relative position and magnetic coupling vary due to

TABLE 2-2
POWER CONDITIONING EQUIPMENT

Component	Quantity
175 μ f 10 kV Energy Storage Capacitor	4
Raytheon Model 107 HV DC Power Supply +2, -6 kV	1
Size D Ignitrons with 1.5 kV Trigger Circuits (LEA 78-1298)	2
Current Sharing Inductor Assemblies 100-110 μ H	16
Flashlamp Circuit Fuses 5kA	16
Current Sharing Inductors (for Diodes) ILC 40 μ H	16
Diode Stacks (15 IR 200IP0200 in series)	8
Ross 3 ϕ Vacuum Interrupter Model HD3-75-100	1
Ross Model HCB-5-C-A SCR Controlled Trip	2
Dummy Load Assemblies (4 Circuits per Assembly)	8
Eight Channel 3nsec - 10 msec Delay Generator with Clock (LEA 79-2601)	1

rotation. The work required to compress this flux is performed by reducing the rotor speed, thereby transforming inertial energy first into magnetic energy, and then to resistive energy in the flashlamp load. It should be noted that the capacitor bank is initially charged to negative 6 kV, and therefore stores less than ten percent of the energy delivered to the flashlamp load.

Equal current division in the flashlamps is achieved by inserting a 100 μ H inductor in series with each lamp. The 5 kA fuses are included in each flashlamp circuit to safely remove a faulted flashlamp from the system in the event of a lamp short circuit.

The vacuum interrupter, not shown in Fig. 2-1, is connected in series with the armature winding and the compensating winding. The vacuum contacts are held open during the period that the rotor is being accelerated from rest to full speed. The close command is given by The University of Texas control console (LDC-40 Line-o-Logic solid state control) after the field circuit is energized and the rotor brushes have been actuated. This reduces the probability of a generator fault and also insures that the brushes do not make a

high current circuit, which could cause damage to the alternator slip rings. The trip command is provided by the LLL eight channel delay generator prior to triggering the flashlamps and series ignitron. This is done to overcome the 12 millisecond delay between the trip command and the time that the contacts part. The timing sequence is designed to be sure that the vacuum breaker opens the circuit before the next positive half cycle after the main discharge. This is necessary because the alternator stores much more energy than could be safely absorbed by the flashlamps. The series diode is provided for the prototype test circuit as a safety measure to allow sufficient deionization margin to be certain that the vacuum breaker contacts will clear successfully.

A dummy resistive load, also not shown in Fig. 2-1, is connected in parallel to the capacitor bank through the second Size D ignitron. The ignitron is triggered if the capacitor bank voltage climbs above a pre-determined value which is set by an adjustable spark gap. If this protective circuit were not included, the capacitor could be pulsed charged to a voltage greater than 10 kV should the flashlamps fail to trigger.

A more detailed circuit schematic diagram is given in Fig. 2-2. The flashlamps, current sharing reactors, startup capacitors, ignitrons, dummy loads, and diode stacks are housed in the LLL equipment racks shown in Fig. 2-3. These racks are located along the southeast wall of the Center for Electromechanics Laboratory (Taylor Hall 179). The engineering prototype compulsator is mounted below floor level in a test pit located in the northeast corner of the lab. The alternator and flashlamp load are connected in series via eight parallel RG 217/U coaxial cables. The top view of the machine is shown in Fig. 2-4. The top of the torque frame and the dc motor drive system are visible above the safety grating which covers the pit area.

Sequence of Operation

The prototype compulsator test circuit is semi-automatic. System startup is manual, with interlocks provided to insure that all necessary requirements are satisfied. The capacitor bank is charged, and the compulsator is motored to speed under manual control. The system is controlled automatically once the discharge sequence is initiated. The sequence of operation of the system for a typical discharge into the flashlamp load is given below.

Figure 2-2. Detailed Power Conditioning Equipment Schematic Diagram.

Figure 2-3. LLL Power Conditioning Equipment Racks.

Figure 2-4. Top View of Prototype Compulsator Test Pit Area.

- I. Startup the compulsator system auxiliaries
 - A. Control power on
 - B. Open main cooling water valves
 - 1. Bearing oil supply heat exchanger
 - 2. Field coil H_2O supply header
 - C. Start oil supply pumps
 - 1. Hydrodynamic bearing supply (low pressure)
 - 2. Hydrostatic lift bearing supply (high pressure)
 - D. Open oil supply valves
 - 1. Lower journal bearing (12 psi)
 - 2. Thrust bearing (12 psi)
 - 3. Upper journal bearing (10 psi)
 - 4. High pressure lift (800 psi)
 - E. Close DC motor drive and motor blower circuit breakers
 - F. Close field coil power supply disconnect switch
- II. Prepare power conditioning equipment
 - A. Control power on
 - B. Open main cooling water valves
 - C. Start de-ionized water supply pump
 - D. Close ignitron heat lamp circuit breaker
 - E. Open nitrogen supply valve to flush amplifier
- III. Set system control parameters
 - A. LDC-40 timers (UT)
 - 1. Field power supply contactor
 - 2. Armature brushgear air supply solenoid
 - 3. Ross circuit breaker close coil
 - B. NOVA minicomputer (UT)
 - 1. UT delay (to allow for buildup of applied field ~ 3.5 sec)
 - 2. Mechanical firing angle (degrees before minimum inductance)
 - 3. LLL delay (to allow Ross breaker to open)
 - C. 8 Channel delay generator (LLL)
 - 1. Ignitron trigger
 - 2. Reflector trigger
 - 3. Ross circuit breaker trip

- 4. Ignitron trigger
- 5-8. Scope triggers
- D. Raytheon HV power supply charge voltage
- IV. Complete second level interlocks
 - A. Charge capacitor bank (-6 kV)
 - B. Motor compulsator to full speed (5400 rpm)
- V. Initiate discharge sequence
 - A. Enable NOVA minicomputer software
 - B. Give discharge command (manual pushbutton)

At this time operation of the system is switched to automatic control, performed by the LDC-40 line-o-logic controller (UT), the NOVA minicomputer (UT), or the eight channel delay generator (LLL). A typical discharge timing sequence is given in Table 2-3 for a 4800 rpm shot. The alternator terminal voltage is used to illustrate the timing sequence in Fig. 2-5.

TABLE 2-3
RUN NUMBER 121 TIMING SIGNALS (4800 RPM)

Timing Signal/Command/Action	Symbol	Elapsed Time (msec)
Initiate discharge	t_0	0
Close field supply contactor	t_0	0
Actuate brush mechanism	t_1	700
Close command to circuit breaker	t_2	1700
Increase speed, time lines of oscillograph	t_3	3400
Compute mechanical period, rotor speed	t_4	3500
Trigger signal to LLL delay generator	t_5	3530
Trip command to Ross circuit breaker	t_6	$t_5 + 0.45$
Trigger oscilloscopes	t_7	$t_5 + 4.28$
Trigger flashlamp reflectors	t_8	$t_5 + 5.28$
Trigger ignitron	t_9	$t_5 + 5.28$
Circuit breaker contacts open	t_{10}	$t_5 + 12$
Reduce speed, time lines of oscillograph	t_{11}	$t_5 + 240$
Open field supply contactor	t_{12}	4000

Figure 2-5. Discharge Timing Sequence - 4800 RPM,

At this point control returns to the operator(s), who perform the following tasks:

1. Dynamic brake ac motor drive
2. Lift brushes from slipring (close air solenoid valve)
Must allow field current and field flux to decay to zero.
3. Shut down system auxiliaries after rotor coasts to a stop.
4. Replace shear pins in drive coupling.

Predicted System Performance

The performance of the prototype compensated pulsed alternator test circuit has been calculated using the Lawrence Livermore Laboratory Mini-Sceptre circuit simulation code (7600 version 1A). The simulated circuit is shown in Fig. 2-6.

The compulsator is modeled as a speed dependent alternating voltage source in series with a time varying inductance and resistance. The inductance L_g is a function of the mechanical position of the rotor and is given by

$$L_g = L_{\min} + \Delta L [1 - \cos(N_p \theta_m / 2)] \quad H \quad (2-1)$$

where $L_{\min}$ is the minimum inductance which occurs at a mechanical angle θ_m equal to zero. N_p is the number of poles of the machine (4). During the simulation studies at both the Center for Electromechanics and Livermore, it has been found that the shape of the generated current pulse is extremely sensitive to the inductance variation ΔL , while the delivered energy is sensitive to the minimum inductance $L_{\min}$.

As will be discussed in Part 3 of this report, the inductance of the machine is controlled by transient eddy currents. These currents flow in the solid poles and stainless steel pole support blocks and in the end turn region of the rotor. These currents are best modeled using an infinite series of transformer coupled resistances, provided that self and mutual inductances and resistance values are calculable.

Our approach to the problem has been to calculate the effective armature inductance based on an assumed boundary condition ($H_r = 0$) at the stator bore. This is reasonable since we know that eddy currents will flow in the solid

$$EAC = V_{max}(\omega/\omega_{max}) \cos [(N_p/2)(\theta_m + \Delta\theta)]$$

$$\frac{d\omega}{dt} = -EAC \times ILG / (\omega J) + 0.5 ILG^2 \left(\frac{dLI_G}{d\theta_m} \right) / J$$

$$ELG = -ILG \times \omega \left(\frac{dLI_G}{d\theta_m} \right)$$

$$LG = L_{min} + \Delta L \left\{ 1 - \cos \left[(N_p/2)(\theta_m) \right] \right\}$$

V_{max} = PEAK VOLTAGE AT $\omega = \omega_{max}$

ω_{max} = MAXIMUM ROTOR ANGULAR VELOCITY

θ_m = MECHANICAL ANGLE (RADIAN)

$\Delta\theta$ = MECHANICAL PHASE ANGLE (RADIAN)

J = ROTOR MOMENT OF INERTIA (J-SEC²)

N_p = NUMBER OF POLES

SCHEMATIC DIAGRAM OF SIMULATED TEST CIRCUIT

Figure 2-6. Schematic Diagram of Simulated Test Circuit.

WLB
UT-CEM
LLL
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conductive materials, which, over a period short compared to a fundamental time constant, behave similarly to a perfect conductor. That is, the armature flux cannot penetrate the stator boundary. On the other hand, the laminated rotor is considered a highly permeable material with low conductivity.

The first calculations of armature inductance for the compensated pulsed alternator were made by Dr. Mircea Driga (UT-CEM) using a finite element method magnetic field mapping computer program (BMAP) that has been developed at The University of Texas. These calculations were made for a conceptual compulsator design that delivered 2.5 megajoules to 196 lamp circuits consisting of two 112 cm lamps connected in series.¹ These calculations considered the non-linear B-H characteristic of the rotor material and the pole faces. The location of the $H_r = 0$ boundary was estimated using equivalent depths of penetration into the poles.

The inductance variation of the engineering prototype compulsator was scaled down from the 2.5 megajoule compulsator design using the relationship

$$A_K = \Delta L / L_{\min} \propto (g/\tau)^2 \quad (2-2)$$

where A_K is the flux compression factor, g is the effective air gap between the armature winding and the compensating winding, and τ is the polar pitch. The minimum inductance of the engineering prototype armature circuit was calculated using Eq. 2-3.

$$L_{\min} = f_1 (\mu_0 / \pi) N_c^2 (\ell_c / D_c) g \quad H \quad (2-3)$$

where μ_0 is the permeability of free space, N_c is the number of conductors, ℓ_c is the average conductor length, and D_c is the average diameter of the windings. The factor f_1 (~ 1.4) is a factor determined from the finite element code calculations for the full scale machine.²

The inductance model used in the final set of simulation runs prior to fabrication of the machine is given by

$$L_g = 27 + 562(1 - \cos 2\theta_m) \quad \mu H \quad (2-4)$$

This expression includes some revisions prompted by additional curvilinear square flux plots made in February, 1979.³

The predicted armature inductance, generator current, total flashlamp current, and delivered energy for the base test circuit case (simulation Z 03/28 10:23:29) are shown in Fig. 2-7 through Fig. 2-10. Important system parameters for this simulation are listed in Table 2-4. These results were considered in our level of confidence in meeting the proposed goal of 180-200 kJ delivered to the sixteen parallel flashlamps.

TABLE 2-4
PREDICTED CIRCUIT PERFORMANCE

Energy delivered to flashlamp load (kJ)	247
Lamp current half width (μ sec)	560
Peak lamp current - total (kA)	72
Peak lamp voltage (kV)	6
Peak generator current (kA)	67
Peak generator voltage (kV)	7.2
Peak mechanical power (MW)	600 @ 548 sec ⁻¹
Change in inertial energy (kJ)	362
Discharge efficiency (247/362) (%)	68

It should be noted that the original simulation model does not include the effects of armature reaction on alternator voltage, nor does it include the resistance of damper circuits in the solid poles and stainless steel support structure. The damper circuits are significant when the coils are uncompensated, but are much less important during the main part of the pulse when the windings are near the minimum inductance position. The effects of armature reaction would be to distort the main field flux lines, but not to decrease the field flux, since the field time constant is much longer than the discharge time.

Measured Armature Circuit Inductance

Since the effective armature inductance is critical in determining system performance, it was considered important to measure the armature circuit

Figure 2-7. Predicted Armature Inductance Versus Time - 5400 RPM.

Figure 2-8. Predicted Compulsator Current Versus Time - 5400 RPM.

Figure 2-9. Predicted Flashlamp Current Versus Time - 5400 RPM.

Figure 2-10. Predicted Energy Delivered to Flashlamps Versus Time - 5400 RPM.

inductance as a function of rotor position. This measurement was made statically using two techniques. First the inductance was measured at 1.0 kHz using a Hewlett-Packard 433A LCR bridge. The results of this measurement are plotted in Fig. 2-11. A second measurement was made by discharging the $700 \text{ }\mu\text{F}$ startup capacitor bank through the alternator at various rotor positions. The effective alternator inductance and resistance calculated by this technique are plotted in Figs. 2-12 and 2-13. Based on both tests we have reached the following conclusions:

1. The minimum inductance is less than or equal to the predicted value of $27 \text{ }\mu\text{H}$.
2. The variation of armature inductance over 90 mechanical degrees is between $6.5:1$ and $10:1$, compared to the original prediction of $40:1$. (Note: Recently, a space harmonic distribution model has been completed which matches the measured values. See Part 3.)
3. The effective armature circuit resistance is increased by a factor of $10:1$ in the uncompensated position, but less than $2:1$ at the point of minimum inductance, by the presence of shorted turns in the solid poles, stainless steel pole supports, and in the end turn region of the rotor.

The armature resistance and inductance were also calculated using a Hewlett-Packard 5420A Fast Fourier Transform digital signal analyzer. The real and imaginary components of the complex impedance (transfer function) were calculated as a function of frequency for various positions of the rotor. Example curves of inductance and resistance as functions of frequency are given in Figs. 2-14 and 2-15 for a rotor position of 105 mechanical degrees (capacitor discharge test number 6-25). These curves are useful in the range from 50 to 800 Hz .

The capacitor discharge tests were repeated at 30 percent and 100 percent field excitation. Results of these tests indicated that the field excitation has negligible effect at the minimum inductance position, and only minor effect at the maximum inductance position. For example, the measured inductance at 85 mechanical degrees from the minimum position was $180 \text{ }\mu\text{H}$ at full field excitation compared to $216 \text{ }\mu\text{H}$ with no field excitation. However, the rotor moved 17 degrees during the test. This motion may have reduced the current pulse width possibly contributing to the lower value of inductance. The rotor motion recorded

Figure 2-11. Armature Inductance Versus Mechanical Angle -
LCR Bridge Measurement at 1.0 kHz.

Figure 2-12. Armature Inductance Versus Mechanical Angle - Capacitor Discharge Tests.

WLB
 UT-CEM
 LLL
 1-15-80

Figure 2-13, Armature Resistance Versus Mechanical Angle - Capacitor Discharge Tests.

WLB
 UT-CEM
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Figure 2-14. Armature Inductance Versus Frequency ($\epsilon_m = 105^\circ$).

Figure 2-15. Armature Resistance Versus Frequency ($\theta_m = 105^\circ$).

over the set of full field excitation capacitor discharges ranged from a few degrees to over one hundred degrees. Therefore, it was not possible to construct a curve of armature inductance versus rotor position for capacitor discharge tests made with the field excited. Values of armature inductance and resistance reduced from capacitor discharge curves are listed in Table 2-5.

TABLE 2-5
EFFECTIVE ARMATURE CIRCUIT INDUCTANCE AND RESISTANCE
CAPACITOR DISCHARGE TESTS 700 μ f 4kV 9/6/79*

Test No.	θ_m (Degrees)	L_{eff} (μ H)	R_{eff} (m Ω)	$f(H_z)$
6-13	0	28	79	1100
6-18	5	29	112	1070
6-33	15	34	109	1000
6-19	15	40	140	909
6-34	20	48	129	842
6-20	30	64	188	711
6-35	45	117	269	525
6-21	60	150	385	447
6-22	80	199	477	381
6-36	85	216	452	374
6-23	90	215	554	356
6-37	95	210	486	372
6-24	100	220	489	365
6-25	105	186	479	390
6-38	110	195	459	388
6-39	120	166	410	423
6-26	130	133	324	485
6-40	160	49	134	821
6-27	180	28	84	1100
6-43	200	44	154	858
6-28	225	107	314	533
6-29	255	211	481	372
6-30	265	226	563	348
6-31	285	198	435	390
6-32	315	110	321	525
6-41	350	35	104	982
6-42	355	30	89	1070

*Includes external series resistance (~ 36 m Ω) and inductance (~ 6 μ H)
0% field excitation

Actual Circuit Performance

The test program was conducted in stages. The compensated pulsed alternator was proven mechanically at each speed before an electrical discharge was made at that speed. Electrical discharges were made at speeds ranging from 1800 rpm to 5400 rpm in 600 rpm increments.

TABLE 2-6
SUCCESSFUL DISCHARGES OF PROTOTYPE COMPULSATOR

Speed (RPM)	Run Numbers of Successful Discharges into Flashlamps
1800	30, 31, 52, 54, 66
2400	55, 60, 69, 70, 72, 93, 116
3000	62, 73, 76, 78
3600	79, 80, 81, 82, 83, 84, 85, 94
4200	119
4800	120, 121
5400	(Terminal Fault, Run No. 122)

The generated output current and delivered energy are determined by multiple control variables, including the mechanical position of the rotor at the time of switching, the time delay between pulsing the reflectors and firing the ignitrons, capacitor voltage, open circuit alternator voltage (field excitation), and rotor speed. The majority of successful discharges were made with the following conditions:

1. θ_m at $t = 0 = -\pi/4$ radians
2. Trigger ignitron and flashlamps simultaneously
3. Charge capacitors to ~ 6 kV
4. Alternator field current 1275 A

The variation in peak output power with speed is plotted in Fig. 2-16. Although lower speed runs are informative and have been useful for debugging purposes, the higher speed runs are most important from the standpoints of specific power and delivered energy.

Figure 2-16. Peak Output Power Versus Rotor Speed.

Photographs of oscilloscope traces for Run No. 119 (4200 rpm) are shown in Figs. 2-17 and 2-18. The alternator terminal voltage (5 kV/div) and current (10 kA/div) are shown in Fig. 2-17. The horizontal time base is set at 1.0 msec/div. The terminal voltage is measured with 1000x attenuation high voltage probes. The current is measured using a Pearson current transformer model 301X. Note that the C.T. saturates at 22 ampere-seconds. This accounts for the rapid fall of the current trace after peak current. (In later shots the generator current is split and measured by two current transformers in an attempt to solve the saturation problem.) In Fig. 2-18 the upper trace is flashlamp voltage (5 kV/div), the middle trace is capacitor current (20 kA/div), and the lower trace is one-fourth of the total flashlamp current (4 of 16 lamps) at 10 kA/div. The sweep time is also 1.0 msec/div. These traces are representative of the characteristic current and voltage waveforms. The high frequency at $t = 0$ is due to discharge of the startup capacitor through both the flashlamps

Figure 2-17. Compulsator Terminal Voltage and Current Waveforms,
Run Number 119 (4200 RPM).

Figure 2-18. Flashlamp Voltage, Capacitor Current, and Flashlamp Current Waveforms; Run Number 119 (4200 RPM).

and the alternator. The lower frequency pulse is attributed to compulsator follow through current by which the bulk of energy is delivered to the flashlamps.

Compulsator Run Number 121 (4800 rpm)

Test Run Number 121 was made December 20, 1979. At 4800 rpm, it is the highest speed discharge obtained on the engineering prototype compulsator prior to the 5400 rpm terminal fault (Run Number 122).

A photograph of the flashlamp voltage, flashlamp current, and capacitor current traces is shown in Fig. 2-19. As before, the upper trace is lamp voltage (5 kv/div), but the middle trace is now one-fourth flashlamp current (4 kA/div), and the bottom two traces are each half the capacitor current (10 kA/div - same base line). The horizontal time base is one millisecond per division. Circuit performance parameters are summarized in Table 2-7.

TABLE 2-7
RUN NUMBER 121 - MEASURED DISCHARGE PARAMETERS

Maximum rotor speed (rpm)	4840
Open circuit voltage (kV)	5.8
Peak terminal voltage (kV)	4.2 (under load)
Peak lamp current (kA)	30.2
Peak lamp power (MW)	111
Time to peak current (msec)	1.39
Pulse half width (msec)	1.3
Delivered energy (kJ)	135

Delivered energy is plotted versus elapsed time in Fig. 2-20. Approximately one quarter of the energy is delivered in the first millisecond, and 90 percent of the energy is delivered within two milliseconds.

Comparison of Measured Waveforms With Computer Simulation Results

The performance of the compulsator as a flashlamp driver is governed by transient, non-linear, electromagnetic diffusion of the magnetic fields in

Figure 2-19. Flashlamp Voltage, Flashlamp Current, and Capacitor Current Waveforms; Run Number 121 (4800 RPM).

Figure 2-20. Energy Delivered to Flashlamps -
Run Number 121 (4800 RPM).

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the machine. Admittedly, the process is complex and to date, only a simplified circuit has been used to model the generator in simulation studies. Therefore, it is important to compare the actual waveforms with those obtained using the computer simulation program. We can then determine if the present model is adequate, and, if not, what modifications to the model are required. Of course, results of the static inductance and resistance measurements on the prototype compulsator must be considered.

The circuit simulation program was used to match the measured flashlamp current and voltage waveforms. Machine and circuit parameter values such as alternator rotor speed, open circuit voltage, capacitor bank voltage, and timing sequence were input to match the actual values as closely as possible. Then, the lamp resistance, series circuit resistance, and compulsator and resistance models were adjusted to best fit the peak current, pulse width and delivered energy measurements. A good match was obtained using the values listed in Table 2-8.

TABLE 2-8
SYSTEM PARAMETERS FOR CIRCUIT SIMULATION - RUN NUMBER 121

Alternator rotor speed (rpm)	4840
Peak open circuit voltage (kV)	5.8
Series resistance ($m\Omega$) (inductors, diodes, and cable)	10
Alternator winding resistance ($m\Omega$)	54
Alternator eddy current resistance ($m\Omega$)	$250(1 - \cos 2\theta_m)$
Armature inductance (μH)	$26 + 78.6(1 - \cos 2\theta_m)$
Time to peak current (msec)	1.49
Peak flashlamp current (kA)	28.2
Peak flashlamp voltage (kV)	3.6
Peak flashlamp power (MW)	102
Energy delivered to flashlamps (kJ)	137

Plots of armature inductance and resistance, flashlamp current and voltage, and energy delivered to the flashlamps are given in Figs. 2-21 to 2-25. The measured values of flashlamp current, voltage, and delivered energy are

Figure 2-21. Run 121 Circuit Simulation - Armature Inductance Versus Time.

Figure 2-22. Run 121 Circuit Simulation - Armature Resistance Versus Time.

Figure 2-23. Run 121 Circuit Simulation - Flashlamp Current Versus Time.

Figure 2-24. Run 121 Circuit Simulation - Flashlamp Voltage Versus Time.

Figure 2-25. Run 121 Circuit Simulation -
Delivered Energy Versus Time.

superimposed on the calculated waveforms for comparison. The actual value of peak current is 1.95 kA higher than the simulated peak current value, and the actual pulse width is slightly less than the predicted pulse width. The calculated delivered energy is 1.5 percent higher than the measured value. The relative height of the positive current peaks appears to be controlled by the magnitude of the alternator eddy current loss. The second or main peak depends on the magnitude of the series circuit resistance primarily on the alternator side of the startup capacitor. The pulse width is sensitive to the initial mechanical angular position of the rotor at the time that the series ignitron is triggered. Peak current and pulse width are also affected by the armature inductance variation.

Summary

The engineering prototype compensated pulsed alternator has been tested in a pulse power circuit designed to deliver 200 kilojoules to a flashlamp load.

The load consists of sixteen 15 x 20 mm x 112 cm long xeron flashlamps connected in parallel. The lamps are housed in a modified 10 cm Beta amplifier. The compulsator was designed to deliver a peak current of 70 kiloamps to the load from a speed of 5400 rpm. The design current half-width is 560 microseconds. The design peak open circuit voltage is 5.8 kilovolts at 5400 rpm.

The prototype compulsator was discharged progressively from speeds of 1800 rpm to 4800 rpm in 600 rpm increments. A total of 27 successful discharges into the flashlamp load were made. Including mechanical tests, the compulsator was motored over 120 times to speeds ranging from 1800 rpm to 5400 rpm. The prototype compulsator generated a maximum output power of over 120 megawatts from 4800 rpm, with the peak power to the flashlamp load approximately 110 megawatts. The peak current to the flashlamp load was 30 kiloamps with a pulse width (half max to half max) of 1300 microseconds. During Experimental Test Run Number 122, a short circuit developed at the alternator terminal internal to the machine at a speed of 5400 rpm. The peak open circuit voltage prior to the fault exceeded six kilovolts. The rotor and stator windings were damaged during the fault to the extent that additional testing was not possible without rewinding the armature conductors.

The reduction in the performance of the pulse power circuit is attributable to several factors. The increase in pulse width is due primarily to the reduced variation in armature inductance. Although the measured value of the minimum inductance is less than or equal to the predicted value, the maximum inductance is a factor of three to four smaller.

The reduction in delivered energy is due to the presence of eddy currents in the solid poles, stainless steel pole supports, and the end turn region of the laminated rotor. The delivered energy is also reduced, to a lesser extent, by a larger series resistance in the coaxial transmission lines, current sharing inductors, and diodes than was assumed in the original circuit simulation.

3. COMPENSATED PULSED ALTERNATOR - ELECTROMAGNETICS

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3. COMPENSATED PULSED ALTERNATOR - ELECTROMAGNETICS

The operation of the Compulsator involves a number of phenomena, the most important of which are:

1. The generation of voltage by the motion of an armature coil through field winding-produced flux.
2. The delivery of output current through a circuit of varying inductance that provides:
 - a. compensation to reduce inductive impedance to current flow; and
 - b. flux compressor by changing inductance to enhance output voltage.
3. The changing magnetic fields may link eddy-current paths that cause added losses in the system.

Because the operation depends so intimately on the magnetic fields in the machine and how they vary in time and space, it is important to have as accurate a magnetic field analysis as practical. In early design studies a computer program was developed that basically uses a finite element technique to iteratively develop a self-consistent solution of Maxwell's equations that takes account of both excitation and boundary conditions. The fields calculated from this program were thought to be quite accurate and they were used to calculate inductance as a function of rotor position. However, the technique is cumbersome in that it requires many computer runs and each one takes considerable time and is costly. As a consequence, this technique is inappropriate for doing extensive parametric studies to seek optimum geometries although it may prove valuable in detailed evaluations of a final design.

A second technique for field analysis was introduced and appears to be much more convenient for doing extensive parametric studies. Moreover, as will be described later, this technique may prove to be a valuable design tool in determining the machine geometry necessary to produce specified circuit behavior.

This second technique makes use of the fact that a sinusoid is a solution of Laplace's equation for a two dimensional problem in cylindrical coordinates with radial and azimuthal variations. This fact leads to the conclusion that if the excitation and boundary conditions can be expressed as Fourier series then the field in the machine can also be found as a space Fourier series. Then, because different harmonics are orthogonal, the

calculation of energy stored in the field to find the inductance is greatly simplified.

To proceed to the formal process of calculating fields, the machine geometry to be analyzed is shown in Fig. 3-1. Both armature and compensating windings are shown as occupying certain radial and azimuthal regions defined by the angles and radii. In the winding region the current density is assumed to be uniform for purposes of our analysis, an assumption well-justified by the fact that the conductor is composed of many very small strands in a Litz wire that is adequately transposed.

If, at a particular radius in one of the windings, we plot the current density as a function of angle, we get the distribution of Fig. 3-2. The winding is being represented is denoted by a subscript, for example, for the armature winding the quantities will be denoted by subscript a

$$J_{ma}, \beta_{1a}, \beta_{2a}, \gamma_{1a}, \gamma_{2a}, \alpha_a$$

and, similarly for the compensating winding, subscript c

$$J_{mc}, \beta_{1c}, \beta_{2c}, \gamma_{1c}, \gamma_{2c}, \alpha_c$$

The function described graphically by Fig. 3-2 can be represented by a Fourier series containing only cosine terms because of the even symmetry. Thus, assuming a series of the form

$$J = J_0 + \sum_{n=1}^{\infty} J_n \cos n(\psi - \alpha) \quad 3-1$$

the coefficients are found in the usual way to obtain

$$J_0 = -\frac{J_m}{2\pi} (\beta_2 - \beta_1) \quad 3-2$$

$$J_n = \frac{2J_m}{\pi n} \left\{ \sin n \frac{\beta_1}{2} + (-1)^n \sin n \frac{\beta_2}{2} \left[1 - 2 \cos n(\beta_2 + \gamma_2) \right] \right. \\ \left. + \left(\frac{\beta_2 - \beta_1}{2\pi} \right) \left[\sin n \frac{\beta_1}{2} + (-1)^n \sin n \frac{\beta_2}{2} \left\{ 1 + 2 \cos n(\beta_2 + \gamma_2) \right\} \right] \right\}$$

3-3

Figure 3-1. Geometry of the Compulsator used for
Space Harmonic Distribution Inductance Calculation

Figure 3-2. Developed View of Compulsator Winding Current Sheets for Space Harmonic Distribution Inductance Calculation

The presence of the term J_0 and the complexity of J_n arise from the absence of one conductor (one-half turn) from the β_1 segments of each winding.

The expressions of Eqs. 3-2 and 3-3 have been programmed for ready computation. Similar expressions can easily be derived for other winding configurations; and, in fact, desired Fourier components may determine a winding configuration.

Because the J_0 term is relatively small in our current configuration we will ignore it here. However, the field produced by it is easily calculated and can be if required.

Using the radial variable ρ to denote a location within a current distribution and considering the interior of the machine where the conductors carry current axially and end effects can be neglected, a differential element of the current density can be treated as an axial (z-directed) current sheet, which, for the n^{th} component is

$$\bar{K}_n = \bar{a}_z K_n \cos n(\psi - \alpha) \quad 3-4$$

where $K_n = J_n d\rho$

The next step in our development is to find the field produced in the machine by the current sheet of Eq. 3-4 for each winding and then integrate over both winding distributions to find the total field strength. Because we are dealing with a linear field problem, we can consider each Fourier component for each winding separately and then sum the results later.

To proceed with the analysis we consider only a current sheet described by Eq. 3-4 in an air gap with free-space permeability μ_0 . Except within the current sheet which is infinitesimal ($d\rho$) in thickness, and possibly at the boundaries, there are no currents flowing, in which case

$$\nabla \times \bar{H} = 0 \quad 3-5$$

which allows $\bar{H}$ to be described as

$$\bar{H} = -\nabla U \quad 3-6$$

Because

$$\bar{B} = \mu_0 \bar{H} \quad 3-7$$

and

$$\nabla \cdot \vec{B} = 0 \quad 3-8$$

the potential U must be a solution of Laplace's equation

$$\nabla^2 U = 0 \quad 3-9$$

We are considering fields in the interior of the machine where axial variations of fields can be ignored, thus, we write Laplace's equation for cylindrical coordinates with $\frac{\partial}{\partial z} = 0$ to obtain

$$\nabla^2 U = \frac{1}{r} \frac{\partial}{\partial r} \left(r \frac{\partial U}{\partial r} \right) + \frac{1}{r^2} \frac{\partial^2 U}{\partial \theta^2} = 0 \quad 3-10$$

We next separate variables by assuming a product solution

$$U = R(r) \Theta(\theta) \quad 3-11$$

substituting it in Eq. 3-10 and solving the two resulting differential equations to obtain the general solution

$$U_n = (C r^n + D r^{-n}) (A \sin n\theta + B \cos n\theta) \quad 3-12$$

From this we obtain the general forms of the radial and azimuthal magnetic field intensity

$$H_{rn} = -\frac{\partial U}{\partial r} = n(-C r^{n-1} + D r^{-n-1})(A_n \sin n\theta + B_n \cos n\theta) \quad 3-13$$

$$H_{\theta n} = \frac{1}{r} \frac{\partial U}{\partial \theta} = n(C_n r^{n-1} + D_n r^{-n-1})(-A_n \cos n\theta + B_n \sin n\theta) \quad 3-14$$

For the general current sheet of Eq. 3-4 which is located at $r = \rho$ we identify two regions:

region i: $R_r < r < \rho$	$\left\{ \begin{array}{l} \text{The field is calculated at} \\ \text{a point radially inwards} \\ \text{from the current sheet.} \end{array} \right\}$
region o: $\rho < r < R_s$	$\left\{ \begin{array}{l} \text{The field is calculated at} \\ \text{a point radially outward} \\ \text{from the current sheet.} \end{array} \right\}$

We will use subscripts i and o to denote the region, with respect to the current sheet, in which the field is being calculated.

It will be convenient to next consider boundary conditions and simplify the field expressions.

The Compulsator rotor stack is laminated ferromagnetic material of high permeability; therefore, it is appropriate to assume $\mu \rightarrow \infty$ which imposes the boundary condition at the rotor surface

$$\text{at } r = R_r; H_{\theta i} = 0 \quad 3-15$$

Application of this boundary condition on Eq. 3-14 yields

$$D_{ni} = -C_{ni} R_r^{2n} \quad 3-16$$

and the simplified field expressions for $R_r < r < \rho$

$$H_{rni} = -nC_{ni} r^{n-1} \left(1 + \frac{R_r^{2n}}{r^{2n}} \right) (A_{ni} \sin n\theta + B_{ni} \cos n\theta) \quad 3-17$$

$$H_{\theta ni} = n C_{ni} r^{n-1} \left(1 - \frac{R_r^{2n}}{r^{2n}} \right) (-A_{ni} \cos n\theta + B_{ni} \sin n\theta) \quad 3-18$$

Considering next the boundary condition at the stator surface ($r = R_s$), the poles are solid steel and they have shorted turns of stainless steel around them. Even though the representation is not as accurate as at the surface of the rotor stack, we will for the moment assume for simplicity that the stator has infinite conductivity ($\sigma \rightarrow \infty$) which results in the boundary condition at $r = R_s$

$$H_{ro} = 0 \quad 3-19$$

Application of this boundary condition to Eq. 3-13 yields

$$D_{no} = C_{no} R_s^{2n} \quad 3-20$$

and the simplified field expressions for $\rho < r < R_s$

$$H_{rno} = -n C_{no} r^{n-1} \left(1 - \frac{R_s^{2n}}{r^{2n}} \right) (A_{no} \sin n\theta + B_{no} \cos n\theta) \quad 3-21$$

$$H_{\theta no} = n C_{no} r^{n-1} \left(1 + \frac{R_s^{2n}}{r^{2n}} \right) (-A_{no} \cos n\theta + B_{no} \sin n\theta) \quad 3-22$$

Next, at the current sheet ($r = \rho$) the radial component of $\vec{H}$ must be continuous

$$\text{at } r = \rho: \quad H_{ri} = H_{ro} \quad 3-23$$

The use of this boundary condition with Eq. 3-17 and 3-21 yields

$$A_{ni} = A_{no} = A_n \quad 3-24$$

$$B_{ni} = B_{no} = B_n \quad 3-25$$

$$C_{no} = C_{ni} \frac{(\rho^{2n} + R_s^{2n})}{(\rho^{2n} - R_s^{2n})} \quad 3-26$$

and the further simplified field expressions are

for $R_r < r < \rho$

$$H_{rni} = -n C_{ni} r^{n-1} \left(1 + \frac{R_r^{2n}}{r^{2n}} \right) (A_n \sin n\theta + B_n \cos n\theta) \quad 3-27$$

$$H_{\theta ni} = n C_{ni} r^{n-1} \left(1 - \frac{R_r^{2n}}{r^{2n}} \right) (-A_n \cos n\theta + B_n \sin n\theta) \quad 3-28$$

for $\rho < r < R_s$

$$H_{rno} = -n C_{ni} \frac{(\rho^{2n} - R_s^{2n})}{(\rho^{2n} - R_s^{2n})} r^{n-1} \left(1 - \frac{R_s^{2n}}{r^{2n}} \right) (A_n \sin n\theta + B_n \cos n\theta) \quad 3-29$$

$$H_{\theta no} = n C_{ni} \frac{(\rho^{2n} - R_s^{2n})}{(\rho^{2n} - R_s^{2n})} r^{n-1} \left(1 + \frac{R_s^{2n}}{r^{2n}} \right) (-A_n \cos n\theta + B_n \sin n\theta) \quad 3-30$$

The final boundary condition is imposed by the current sheet at $r = \rho$ and is

$$H_{\theta no} - H_{\theta ni} = K_z = K_n \cos n(\theta - \alpha) \quad 3-31$$

The use of this equation with Eqs. 3-28 and 3-30 yields the constants

$$A_n C_{ni} = - \frac{K_n (\rho^{2n} - R_s^{2n})}{2n \rho^{n-1} (R_s^{2n} + R_r^{2n})} \cos n\alpha \quad 3-32$$

$$B_n C_{ni} = \frac{K_n (\rho^{2n} - R_s^{2n})}{2n \rho^{n-1} (R_s^{2n} + R_r^{2n})} \sin n\alpha \quad 3-33$$

The use of these constants in Eqs. 3-27 through 3-30 yield the further simplified field expressions

for $R_r < r < \rho$

$$H_{rni} = \frac{K_n r^{n-1} \left(1 + \frac{R_r^{2n}}{r^{2n}} \right) (\rho^{2n} - R_s^{2n})}{2(R_s^{2n} + R_r^{2n}) \rho^{n-1}} \sin n(\theta - \alpha) \quad 3-34$$

$$H_{\theta ni} = \frac{K_n r^{n-1} \left(1 - \frac{R_s^{2n}}{r^{2n}}\right) (\rho^{2n} - R_s^{2n})}{2(R_s^{2n} + R_r^{2n}) \rho^{n-1}} \cos n(\theta - \alpha) \quad 3-35$$

for $\rho < r < R_s$

$$H_{rno} = \frac{K_n r^{n-1} \left(1 - \frac{R_s^{2n}}{r^{2n}}\right) (\rho^{2n} + R_r^{2n})}{2(R_s^{2n} + R_r^{2n}) \rho^{n-1}} \sin n(\theta - \alpha) \quad 3-36$$

$$H_{\theta no} = \frac{K_n r^{n-1} \left(1 + \frac{R_s^{2n}}{r^{2n}}\right) (\rho^{2n} + R_r^{2n})}{2(R_s^{2n} + R_r^{2n}) \rho^{n-1}} \cos n(\theta - \alpha) \quad 3-37$$

To calculate the total field due to a single winding, we make the replacement (see Eq. 3-4)

$$K_n \Rightarrow J_n d\rho$$

which makes the fields of Eqs. 3-34 through 3-37 differentials

$$dH_{rni}, dH_{\theta ni}, dH_{rno}, dH_{\theta no}$$

The differentials are then integrated over the total winding to find the total field.

Consider first the compensating winding which, according to Fig. 3-1 occupies the radial space

$$R_5 < \rho < R_6$$

For this winding we identify three regions in which the field must be calculated separately as follows:

for $R_4 < r < R_5$

$$H_{nc} = \int_{R_5}^{R_6} d H_{nic} \quad 3-38$$

for $R_5 < r < R_6$

$$H_{nc} = \int_{R_5}^r d H_{noc} + \int_r^{R_6} d H_{nic} \quad 3-39$$

for $R_6 < r < R_5$

$$H_{nc} = \int_{R_5}^{R_6} d H_{noc} \quad 3-40$$

The use of these integrals with the field expressions of Eqs. 3-34 through 3-37 leads to the field expressions for the compensating winding given in Table 3-1. Note that it was necessary to integrate separately for $n \neq 2$ and $n = 2$.

Because the process is exactly the same for the armature winding, the field expressions for the armature winding in Table 3-1 are obtained from those for the compensating windings by making the replacements:

$$J_{nc} \Rightarrow J_{na}$$

$$\alpha_c \Rightarrow \alpha_a$$

$$R_5 \Rightarrow R_1$$

$$R_6 \Rightarrow R_2$$

TABLE 3-1
MAGNETIC FIELD INTENSITY

Region	Compensating Winding
$R_6 < r < R_5$	$H_{rnc} = \frac{A_{nc} J_{nc}}{2B_n} r^{n-1} \left(1 - \frac{R_s^{2n}}{r^{2n}} \right) \sin n(\theta - \alpha_c)$ $H_{\theta nc} = \frac{A_{nc} J_{nc}}{2B_n} r^{n-1} \left(1 + \frac{R_s^{2n}}{r^{2n}} \right) \cos n(\theta - \alpha_c)$
$R_5 < r < R_6$	$n \neq 2: H_{rnc} = \frac{J_{nc}}{2B_n} \left[C_{nc} r^{n-1} + \left(\frac{2n}{4-n^2} \right) B_n r + D_{nc} r^{-n-1} \right] \sin n(\theta - \alpha_c)$ $n = 2: H_{r2c} = \frac{J_{2c}}{2B_2} \left[\left(C_{2c} - \frac{B_2}{4} \right) r + B_2 (r \ln r) + D_{2c} r^{-3} \right] \sin 2(\theta - \alpha_c)$ $n \neq 2: H_{\theta nc} = \frac{J_{nc}}{2B_n} \left[C_{nc} r^{n-1} + \left(\frac{4}{4-n^2} \right) B_n r - D_{nc} r^{-n-1} \right] \cos n(\theta - \alpha_c)$ $n = 2: H_{\theta 2c} = \frac{J_{2c}}{2B_2} \left[\left(C_{2c} + \frac{B_2}{4} \right) r + B_2 (r \ln r) - D_{2c} r^{-3} \right] \cos 2(\theta - \alpha_c)$
$R_r < r < R_5$	$H_{rnc} = \frac{E_{nc} J_{nc}}{2B_n} r^{n-1} \left(1 + \frac{R_r^{2n}}{r^{2n}} \right) \sin n(\theta - \alpha_c)$ $H_{\theta nc} = \frac{E_{nc} J_{nc}}{2B_n} r^{n-1} \left(1 - \frac{R_r^{2n}}{r^{2n}} \right) \cos n(\theta - \alpha_c)$

Armature Winding	Region
$H_{rna} = \frac{A_{na} J_{na}}{2B_n} r^{n-1} \left(1 - \frac{R_s^{2n}}{r^{2n}} \right) \sin n(\theta - \alpha_a)$ $H_{\theta na} = \frac{A_{na} J_{na}}{2B_n} r^{n-1} \left(1 + \frac{R_s^{2n}}{r^{2n}} \right) \cos n(\theta - \alpha_a)$	$R_2 < r < R_s$
$n \neq 2: H_{rna} = \frac{J_{na}}{2B_n} \left[C_{na} r^{n-1} + \left(\frac{2n}{4-n^2} \right) B_n r + D_{na} r^{-n-1} \right] \sin n(\theta - \alpha_a)$ $n = 2: H_{r2a} = \frac{J_{2a}}{2B_2} \left[\left(C_{2a} - \frac{B_2}{4} \right) r + B_2 (r \ln r) + D_{2a} r^{-3} \right] \sin 2(\theta - \alpha_a)$ $n \neq 2: H_{\theta na} = \frac{J_{na}}{2B_n} \left[C_{na} r^{n-1} + \left(\frac{4}{4-n^2} \right) B_n r - D_{na} r^{-n-1} \right] \cos n(\theta - \alpha_a)$ $n = 2: H_{\theta 2a} = \frac{J_{2a}}{2B_2} \left[\left(C_{2a} + \frac{B_2}{4} \right) r + B_2 (r \ln r) - D_{2a} r^{-3} \right] \cos 2(\theta - \alpha_a)$	$R_1 < r < R_2$
$H_{rna} = \frac{E_{na} J_{na}}{2B_n} r^{n-1} \left(1 + \frac{R_r^{2n}}{r^{2n}} \right) \sin n(\theta - \alpha_a)$ $H_{\theta na} = \frac{E_{na} J_{na}}{2B_n} r^{n-1} \left(1 - \frac{R_r^{2n}}{r^{2n}} \right) \cos n(\theta - \alpha_a)$	$R_r < r < R_1$

Compensating Winding Constants

$$n \neq 2$$

$$A_{nc} = \frac{R_6^{n+2} - R_5^{n+2}}{n+2} + R_r^{2n} \left(\frac{R_6^{2-n} - R_5^{2-n}}{2-n} \right)$$

$$B_n = R_s^{2n} + R_r^{2n}$$

$$C_{nc} = \frac{R_6^{n+2} - R_5^{n+2}}{n+2} - \left(\frac{R_s^{2n} R_6^{2-n} + R_r^{2n} R_5^{2-n}}{2-n} \right)$$

$$D_{nc} = \frac{R_s^{2n} R_5^{n+2} + R_r^{2n} R_6^{n+2}}{n+2} - \frac{R_s^{2n} R_r^{2n}}{(2-n)} (R_6^{2-n} - R_5^{2-n})$$

$$E_{nc} = \frac{R_6^{n+2} - R_5^{n+2}}{n+2} - R_s^{2n} \left(\frac{R_6^{2-n} - R_5^{2-n}}{2-n} \right)$$

$$n = 2$$

$$A_{2c} = \frac{R_6^4 - R_5^4}{4} + R_r^4 \ln \frac{R_6}{R_5}$$

$$B_2 = R_s^4 + R_r^4$$

$$C_{2c} = \frac{R_6^4 - R_5^4}{4} - (R_s^4 \ln R_6 + R_r^4 \ln R_5)$$

$$D_{2c} = \frac{R_s^4 R_5^4 + R_r^4 R_6^4}{4} - R_s^4 R_r^4 \ln \frac{R_6}{R_5}$$

$$E_{2c} = \frac{R_6^4 - R_5^4}{4} - R_s^4 \ln \frac{R_6}{R_5}$$

Armature Winding Constants

$n \neq 2$

$$A_{na} = \frac{R_2^{n+2} - R_1^{n+2}}{n+2} + R_r^{2n} \left(\frac{R_2^{2-n} - R_1^{2-n}}{2-n} \right)$$

$$B_n = R_s^{2n} + R_r^{2n}$$

$$C_{na} = \frac{R_2^{n+2} - R_1^{n+2}}{n+2} - \left(\frac{R_s^{2n} R_2^{2-n} + R_r^{2n} R_1^{2-n}}{2-n} \right)$$

$$D_{na} = \frac{R_s^{2n} R_1^{n+2} + R_r^{2n} R_2^{n+2}}{n+2} - \frac{R_s^{2n} R_r^{2n}}{(2-n)} (R_2^{2-n} - R_1^{2-n})$$

$$E_{na} = \frac{R_2^{n+2} - R_1^{n+2}}{n+2} - R_s^{2n} \left(\frac{R_2^{2-n} - R_1^{2-n}}{2-n} \right)$$

$n = 2$

$$A_{2a} = \frac{R_2^4 - R_1^4}{4} + R_r^4 \ln \frac{R_2}{R_1}$$

$$B_2 = R_s^4 + R_r^4$$

$$C_{2a} = \frac{R_2^4 - R_1^4}{4} - (R_s^4 \ln R_2 + R_r^4 \ln R_1)$$

$$D_{2a} = \frac{R_s^4 R_1^4 + R_r^4 R_2^4}{4} - R_s^4 R_r^4 \ln \frac{R_2}{R_1}$$

$$E_{2a} = \frac{R_2^4 - R_1^4}{4} - R_s^4 \ln \frac{R_2}{R_1}$$

The next step in our analysis is to use the field intensities of Table 3-1 to calculate the stored magnetic energy and from that the self and mutual inductances of the windings. Basically, the magnetic stored energy is calculated from

$$W_m = \int_{vol} \frac{1}{2} \mu_0 H^2 d vol \quad 3-41$$

It will be convenient to calculate self and mutual inductances separately. The differential volume element is

$$d vol = \ell r dr d\theta \quad 3-42$$

where ℓ is the effective axial length. Then for either winding

$$W_m = \int_0^{2\pi} \int \frac{1}{2} \mu_0 (H_r^2 + H_\theta^2) \ell r dr d\theta \quad 3-43$$

Because different harmonics are orthogonal with respect to the integration on θ , all cross terms will integrate to zero and Eq. 3-43 can be written as

$$W_m = \sum_{n=1}^{\infty} W_{mn} \quad 3-44$$

with

$$W_{mn} = \int_0^{2\pi} \int \frac{1}{2} \mu_0 (H_{rn}^2 + H_{\theta n}^2) \ell r dr d\theta \quad 3-45$$

With reference to Table 3-1 each field component can always be written in the form

$$H_{rn} = H_{rnm} \sin n(\theta - \alpha) \quad 3-46$$

$$H_{\theta n} = H_{\theta nm} \cos n(\theta - \alpha) \quad 3-47$$

Squaring these expressions and replacing the squares of the sinusoidal functions with trigonometric identities leads to

$$W_{mn} = \int_0^{2\pi} \int \frac{1}{2} \mu_0 \left\{ H_{rnm}^2 \left(\frac{1}{2} \right) [1 - \cos 2n(\theta - \alpha)] + H_{\theta nm}^2 \left(\frac{1}{2} \right) [1 + \cos 2n(\theta - \alpha)] \right\} r dr d\theta \quad 3-48$$

Carrying out the integration on θ yields zero for the sinusoidal terms leaving

$$W_{mn} = \frac{\pi \mu_0 l^2}{2} \int (H_{rnm}^2 + H_{\theta nm}^2) r dr \quad 3-49$$

Using the field expressions and constants defined in Table 3-1, and Eq. 3-49, the stored energies for the various regions for the armature and compensating winding excited separately were calculated and are displayed in Table 3-2.

Using the fact that for self inductance

$$W_m = \frac{1}{2} L i^2 \quad 3-50$$

the energies given in Table 3-2 along with the relation

$$J_{m11c} = \frac{N_{11} i_c}{\pi (R_6^2 - R_5^2) \beta_{1c} / 360^\circ} \quad 3-51a$$

$$J_{m11a} = \frac{N_{11} i_a}{\pi (R_2^2 - R_1^2) \beta_{1a} / 360^\circ} \quad 3-51b$$

yields the self inductances in Table 3-3, for use in the expression

$$L = \sum_{n=1}^{\infty} L_n \quad 3-52$$

TABLE 3-2

MAGNETIC STORED ENERGY IN SELF INDUCTANCES

 $n \neq 2$

j		Comp Wind	Arm Wind	Region
1	$R_r < r < R_5$	$W_{mnc} = \frac{\pi \mu_0 \ell J_{nc}^2 E_{nc}^2}{8n B_n^2} [R_5^{2n} - R_r^{4n} R_5^{-2n}]$	$W_{mna} = \frac{\pi \mu_0 \ell J_{na}^2 E_{na}^2}{8n B_n^2} [R_1^{2n} - R_r^{4n} R_1^{-2n}]$	$R_r < r < R_1$
2	$R_5 < r < R_6$	$W_{mnc} = \frac{\pi \mu_0 \ell J_{nc}^2}{8 B_n^2} \left[\frac{(n^2+4) B_n^2}{(4-n^2)^2} (R_6^4 - R_5^4) \right.$ $+ \frac{C_{nc}^2}{n} (R_6^{2n} - R_5^{2n})$ $- \frac{D_{nc}^2}{n} (R_6^{-2n} - R_5^{-2n})$ $+ \frac{4 B_n^2}{(4-n^2)} \left\{ C_{nc} (R_6^{n+2} - R_5^{n+2}) \right.$ $\left. \left. - D_{nc} (R_6^{2-n} - R_5^{2-n}) \right\} \right]$	$W_{mna} = \frac{\pi \mu_0 \ell J_{na}^2}{8 B_n^2} \left[\frac{(n^2+4) B_n^2}{(4-n^2)^2} (R_2^4 - R_1^4) \right.$ $+ \frac{C_{na}^2}{n} (R_2^{2n} - R_1^{2n})$ $- \frac{D_{na}^2}{n} (R_2^{-2n} - R_1^{-2n})$ $+ \frac{4 B_n^2}{(4-n^2)} \left\{ C_{na} (R_2^{n+2} - R_1^{n+2}) \right.$ $\left. \left. - D_{na} (R_2^{2-n} - R_1^{2-n}) \right\} \right]$	$R_1 < r < R_2$
3	$R_6 < r < R_s$	$W_{mnc} = \frac{\pi \mu_0 \ell J_{nc}^2 E_{nc}^2}{8n B_n^2}$ $\times [R_s^{4n} R_6^{-2n} - R_6^{2n}]$	$W_{mna} = \frac{\pi \mu_0 \ell J_{na}^2 E_{na}^2}{8n B_n^2}$ $\times [R_s^{4n} R_2^{-2n} - R_2^{2n}]$	$R_2 < r < R_s$

TABLE 3-2 (continued)

MAGNETIC STORED ENERGY IN SELF INDUCTANCES

$$n = 2$$

j		Comp Wind	Arm Wind	Region
1	$R_r < r < R_5$	$W_{m2c} = \frac{\pi \mu_0^2 J_{2c}^2 E_{2c}^2}{16 B_2^2} [R_5^4 - R_r^8 R_5^{-4}]$	$W_{m2a} = \frac{\pi \mu_0^2 J_{2a}^2 E_{2a}^2}{16 B_2^2}$ $\times [R_1^4 - R_r^8 R_1^{-4}]$	$R_r < r < R_1$
2	$R_5 < r < R_6$	$W_{m2c} = \frac{\pi \mu_0^2 J_{2c}^2}{8 B_n^2} \left[\left(\frac{(4C_{2c} - B_2)^2}{32} \right. \right.$ $\left. \left. + \frac{B_2^2}{16} \right) (R_6^4 - R_5^4) - D_{2c} (R_6^{-4} - R_5^{-4}) \right.$ $\left. + \frac{B_2^2}{2} \left(R_6^4 (\ln R_6)^2 - R_5^4 (\ln R_5)^2 \right) \right.$ $\left. + B_2 (C_{2c} - \frac{B_2}{4}) (R_6^4 \ln R_6 - R_5^4 \ln R_5) \right.$ $\left. - B_2 D_{2c} \ln \left(\frac{R_6}{R_5} \right) \right]$	$W_{m2a} = \frac{\pi \mu_0^2 J_{2a}^2}{8 B_n^2} \left[\left(\frac{(4C_{2a} - B_2)^2}{32} \right. \right.$ $\left. \left. + \frac{B_2^2}{16} \right) (R_2^4 - R_1^4) - D_{2a} (R_2^{-4} - R_1^{-4}) \right.$ $\left. + \frac{B_2^2}{2} \left(R_2^4 (\ln R_2)^2 - R_1^4 (\ln R_1)^2 \right) \right.$ $\left. + B_2 (C_{2a} - \frac{B_2}{4}) (R_2^4 \ln R_2 - R_1^4 \ln R_1) \right.$ $\left. - B_2 D_{2a} \ln \left(\frac{R_2}{R_1} \right) \right]$	$R_1 < r < R_2$
3	$R_6 < r < R_5$	$W_{m2c} = \frac{\pi \mu_0^2 J_{2c}^2 A_{2c}^2}{16 B_n^2} [R_5^8 R_6^{-4} - R_6^4]$	$W_{m2a} = \frac{\pi \mu_0^2 J_{2a}^2 A_{2a}^2}{16 B_n^2} [R_5^8 R_2^{-4} - R_2^4]$	$R_2 < r < R_5$

TABLE 3-3
SELF INDUCTANCES

Compensating Winding

$$n \neq 2: L_{nc} = 2 \left[\sum_{j=1}^3 \left(\frac{W_{mnc}}{i_c^2} \right) \right]$$

$$n = 2: L_{2c} = 2 \left[\sum_{j=1}^3 \left(\frac{W_{mn2}}{i_c^2} \right) \right]$$

Armature Winding

$$n \neq 2: L_{na} = 2 \left[\sum_{j=1}^3 \left(\frac{W_{mna}}{i_a^2} \right) \right]$$

$$n = 2: L_{2a} = 2 \left[\sum_{j=1}^3 \left(\frac{W_{mn2}}{i_a^2} \right) \right]$$

The final calculation to be made is of the mutual inductance between the armature and compensating windings. This requires the calculation of magnetic stored energy resulting only from simultaneous excitation of both windings. From Eq. 3-43 these cross terms yield

$$W_m = \int_0^{2\pi} \int \mu_0 (H_{rc} H_{ra} + H_{\theta c} H_{\theta a}) r dr d\theta \quad 3-53$$

Using orthogonality of different Fourier components and the general form of the field expressions given in Eqs. 3-46 and 3-47 is with the n^{th} component given by

$$W_{mn} = \int_0^{2\pi} \int \mu_0 [H_{rnm} c H_{rnm} a \sin n(\theta - \alpha_c) \sin n(\theta - \alpha_a) + H_{\theta nm} c H_{\theta nm} a \cos n(\theta - \alpha_c) \cos n(\theta - \alpha_a)] r dr d\theta \quad 3-54$$

Using trigonometric identities on the products of the sinusoidal functions and integrating on θ yields

$$W_{mn} = \pi \mu_0 l \int (H_{rnm} c H_{rnm} a + H_{\theta nm} c H_{\theta nm} a) \cos (\alpha_a - \alpha_c) r dr \quad 3-55$$

The use of the field expressions of Table 3-1 with this equation results in the mutual energy storage in the five regions as presented in Table 3-4. The combination of these energy expressions, the use of Eqs. 3-51a and 3-51b, the expression

$$W_{mm} = M i_c i_a \quad 3-56$$

and

$$M = \sum_{n=1}^{\infty} M_n \cos n(\alpha_a - \alpha_c) \quad 3-57$$

TABLE 3-4
MAGNETIC STORED ENERGY IN MUTUAL INDUCTANCE

k	Region	$n \neq 2$
1	$R_r < r < R_1$	$W_{mn} = \frac{\pi \nu_0 \epsilon J_{nc} J_{na} E_{nc} E_{na}}{2B_n^2} \left[\frac{R_1^{2n} - R_r^{4n} R_1^{-2n}}{2n} \right] \cos n(\alpha_a - \alpha_c)$
2	$R_1 < r < R_2$	$W_{mn} = \frac{\pi \nu_0 \epsilon J_{nc} J_{na} E_{nc}}{4B_n^2} \left[\left(\frac{2}{4-n} \right) B_n \left\{ (R_n^{n+2} - R_1^{n+2}) - R_r^{2n} (R_2^{2-n} - R_1^{2-n}) \right\} \right. \\ \left. + C_{na} \frac{(R_2^{2n} - R_1^{2n})}{n} - D_{na} \frac{R_r^{2n} (R_2^{-2n} - R_1^{-2n})}{n} \right] \cos n(\alpha_a - \alpha_c)$
3	$R_2 < r < R_5$	$W_{mn} = \frac{\pi \nu_0 \epsilon J_{nc} J_{na} E_{nc} A_{na}}{2B_n^2} \left[\left(\frac{R_5^{2n} - R_2^{2n}}{2n} \right) + \frac{R_5^{2n} R_r^{2n} (R_5^{-2n} - R_2^{-2n})}{2n} \right] \cos n(\alpha_a - \alpha_c)$
4	$R_5 < r < R_6$	$W_{mn} = \frac{\pi \nu_0 \epsilon J_{nc} J_{na} A_{na}}{4B_n^2} \left[\left(\frac{2}{4-n} \right) B_n \left\{ (R_6^{n+2} - R_5^{n+2}) \right. \right. \\ \left. \left. + R_5^{2n} (R_6^{2-n} - R_5^{2-n}) \right\} + \frac{C_{nc} (R_6^{2n} - R_5^{2n})}{n} \right. \\ \left. + \frac{D_{nc}}{n} R_5^{2n} (R_6^{-2n} - R_5^{-2n}) \right] \cos n(\alpha_a - \alpha_c)$
5	$R_6 < r < R_s$	$W_{mn} = \frac{\pi \nu_0 \epsilon J_{nc} A_{nc} J_{na} A_{na}}{2B_n^2} \left[\frac{R_s^{4n} R_6^{-2n} - R_6^{2n}}{2n} \right] \cos(\alpha_a - \alpha_c)$

TABLE 3-4 (continued)
MAGNETIC STORED ENERGY IN MUTUAL INDUCTANCE

k	Region	n = 2
1	$R_r < r < R_1$	$W_{m2} = \frac{\pi \mu_0 \epsilon J_{2c} J_{2a} E_{2c} E_{2a}}{2B_2^2} \left[\frac{R_1^4 - R_r^4}{4} \right] \cos 2(\alpha_a - \alpha_c)$
2	$R_1 < r < R_2$	$W_{m2} = \frac{\pi \mu_0 \epsilon J_{2c} J_{2a} E_{2c} E_{2a}}{2B_2^2} \left[C_{2a} \frac{(R_2^4 - R_1^4)}{4} \right. \\ \left. + \frac{B_2}{4} \left\{ (R_2^4 \ln R_2 - R_1^4 \ln R_1) - \frac{(R_2^4 - R_1^4)}{4} \right\} \right. \\ \left. - R_r^4 \frac{B_2}{4} \ln \left(\frac{R_2}{R_1} \right) - \frac{D_{2a} R_r^4 (R_2^{-4} - R_1^{-4})}{4} \right] \cos 2(\alpha_a - \alpha_c)$
3	$R_2 < r < R_5$	$W_{m2} = \frac{\pi \mu_0 \epsilon J_{2c} J_{2a} E_{2c} E_{2a}}{2B_2^2} \left[\frac{(R_5^4 - R_2^4)}{4} + \frac{R_5^4 R_r^4 (R_5^{-4} - R_2^{-4})}{4} \right] \cos 2(\alpha_a - \alpha_c)$
4	$R_5 < r < R_6$	$W_{m2} = \frac{\pi \mu_0 \epsilon J_{2c} J_{2a} E_{2c} E_{2a}}{2B_2^2} \left[C_{2c} \frac{(R_6^4 - R_5^4)}{4} \right. \\ \left. + \frac{B_2}{4} \left\{ (R_6^4 \ln R_6 - R_5^4 \ln R_5) - \frac{(R_6^4 - R_5^4)}{4} \right\} \right. \\ \left. + R_s^4 \frac{B_2}{4} \ln \left(\frac{R_6}{R_5} \right) + D_{2c} R_s^4 \frac{(R_6^{-4} - R_5^{-4})}{4} \right] \cos 2(\alpha_a - \alpha_c)$
5	$R_6 < r < R_s$	$W_{m2} = \frac{\pi \mu_0 \epsilon J_{2c} J_{2a} E_{2c} E_{2a}}{2B_2^2} \left[\frac{R_s^4 - R_6^4}{4} \right] \cos 2(\alpha_a - \alpha_c)$

yields

$$n \neq 2: \quad M_n = \sum_{k=1}^5 \frac{W_{mn}}{a^k c} \quad 3-58$$

$$n = 2: \quad M_2 = \sum_{k=1}^5 \frac{W_{m2}}{a^k c} \quad 3-59$$

When the armature and compensating windings are connected in series the total inductance of the circuit is

$$L = L_a + L_c + 2M \quad 3-60$$

which, with the use of Eq. 3-57 becomes

$$L = L_a + L_c + 2 \sum_{n=1}^{\infty} M_n \cos n(\alpha_a - \alpha_c) \quad 3-61$$

which describes explicitly how the circuit inductance varies with the relative angular position $(\alpha_a - \alpha_c)$ of the two windings.

Calculated Armature Inductance for Prototype Compulsator

The space harmonic distribution technique to calculate the magnetic field strengths, magnetic energy, and effective inductance was developed after the engineering prototype compulsator had been designed and fabricated. Therefore, the exact winding configuration and system geometry were readily available to use in calculating the effective inductance of the machine. In addition, the inductance of the prototype compulsator had been measured as a function of rotor position using two methods that yielded comparable results. Therefore, the calculated inductance could be easily checked with actual inductance measurements.

The actual radial and angular position of each belt of the windings is shown in Figure 3-3. The origin $(\psi - \alpha) = 0$ corresponds to the center line of the belt of the windings that has the missing conductor, which is the result of the serpentine winding configuration. The winding has 47 passes or conductors connected in series with 12 conductors per pole for three of the poles

GEOMETRY OF THE COMPULSATOR FOR S.H.D. INDUCTANCE CALCULATION

$R_r = 18.957 \text{ cm}$
 $R_1 = 19.402 \text{ cm}$
 $R_2 = 19.910 \text{ cm}$
 $R_5 = 21.135 \text{ cm}$
 $R_5' = 21.961 \text{ cm}$
 $R_6 = 20.314 \text{ cm}$
 $R_6 = 20.822 \text{ cm}$

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Figure 3-3. Definition of Engineering Prototype Compulsator Parameters for Inductance Calculation

and 11 conductors for the remaining pole. Other parameters are listed in Table 3-5.

TABLE 3-5
PROTOTYPE COMPULSATOR WINDING CONFIGURATION FOR INDUCTANCE CALCULATIONS

Parameter	Definition	Dimension
β_{1a}	Span - 11 Conductor Armature Winding Belt	67.3°
β_{1c}	Span - 11 Conductor Compensating Winding Belt	64.36°
β_{2a}	Span - 12 Conductor Armature Winding Belt	73.4°
β_{2c}	Span - 12 Conductor Compensating Winding Belt	70.2°
γ_{1a}	Span - Narrow Armature Winding Spacers	17.3°
γ_{1c}	Span - Narrow Compensating Winding Spacers	20.3°
γ_{2a}	Span - Wide Armature Winding Spacers	19.0°
γ_{2c}	Span - Wide Compensating Winding Spacers	22.2°
R_r	Outer Radius - Rotor Laminations (cm)	18.96
R_1	Inner Radius - Armature Conductor (cm)	19.40
R_2	Outer Radius - Armature Winding Conductor (cm)	19.91
R_5	Inner Radius - Compensating Conductor (cm)	20.31
R_6	Outer Radius - Compensating Conductor (cm)	20.82
R_s	Inner Radius of Pole (cm)	21.14
R_s'	Effective $H_r = 0$ Boundary (cm)	21.96

For the first set of calculations, the effective $H_r = 0$ boundary, R_s' , was set equal to the inner radius of the stator pole, R_s . The self inductance of the armature winding, L_a , the self inductance of the compensating winding, L_c , and the mutual inductance, M , were then calculated. The values calculated were

$$L_a = 43.8 \mu H$$

$$L_c = 15.6 \mu H$$

$$M(\alpha_a - \alpha_c) = -17.8 \mu H \quad \text{at } (\alpha_a - \alpha_c) = 0, \quad i_c = -i_a$$

$$M(\alpha_a - \alpha_c) = +17.8 \mu H \quad \text{at } (\alpha_a - \alpha_c) = \pi/2, \quad i_c = -i_a$$

The effective minimum and maximum inductances were then calculated using Eqs. 3-62 and 3-63.

$$L_{\min} = L_a + L_c + 2M(0) = 24.0 \mu H \quad 3-62$$

$$L_{\max} = L_a + L_c + 2M(\pi/2) = 95.0 \mu H \quad 3-63$$

Although the minimum inductance compared reasonably well with the measured value of 26.3 μH , the maximum inductance was approximately one-half the measured value of 176 μH . (The actual values were determined by the LCR Bridge measurement at 1.0 kHz.)

Since the poles are not perfect conductors, however, there will be some penetration of the field into the pole region. Therefore, the effective $H_r = 0$ boundary was moved radially outward from the stator bore in small increments until a match was obtained. An effective $H_r = 0$ boundary (assuming $\mu = \mu_0$ in the region between R_s and R_s') located at a depth of 0.71 cm into the poles yielded results that matched the measured values. The values calculated with R_s' equal to $R_s + 0.71$ cm are given below:

$$L_a = 64.0 \mu H$$

$$L_c = 37.2 \mu H$$

$$M(0) = -38.6 \mu H$$

$$M(\pi/2) = +38.6 \mu H$$

$$L_{\min} = 24.1 \mu H$$

$$L_{\max} = 178 \mu H$$

For all successive calculations, it is assumed that the effective boundary R_s' is equal to the inner pole radius R_s plus 0.71 cm.

To determine if this adjustment is reasonable, it is useful to calculate the equivalent depth in air that results in the same permeance as

that calculated using the depth of penetration in the ferromagnetic pole material. The depth of penetration under steady state conditions is given by Eq. 3-64.¹

$$\delta = (\pi f \mu \sigma)^{-1/2} \quad (\text{m}) \quad 3-64$$

where f is the frequency of the ac current, μ is the magnetic permeability of the material ($\mu = \mu_r \mu_0$), and σ is the electrical conductivity of the material. The relative permeability ($B/\mu_0 H$) and the incremental relative permeability ($\Delta B/\mu_0 \Delta H$) are determined by the magnitude of the magnetic flux density. Assuming a conductivity σ of 13% IACS, and a frequency of 1.0 kHz, the depth of penetration may be calculated for a given relative incremental permeability. The effective permeance of the steel pole tip with eddy currents flowing in a skin depth δ is approximated by an additional layer of air of thickness δ' given by

$$\delta' = (\mu_r)_{\text{inc}} \delta/3 = R'_s - R_s \quad 3-65$$

The factor of one third is used to account for the magnetic field strength decreasing linearly over the depth of penetration, assuming that the eddy currents are uniformly distributed. Values of δ' for various flux densities in the steel pole region are given in Table 3-6. The magnetization curve used for this analysis is fit to the B-H curves for cast steel given in Attwood.²

TABLE 3-6
VALUES OF $\delta' = R'_s - R_s$ VS MAGNETIC FLUX DENSITY B

B(T)	μ_r	$(\mu_r)_{\text{inc}}$	$\delta(\text{cm})$	$\delta' = R'_s - R_s(\text{cm})$
1.6	260	35.4	0.10	1.15
1.9	78	9.3	0.19	0.59
2.3	19	2.2	0.39	0.29
2.5	10	1.2	0.52	0.21

The value of $\delta' = 0.71$ required to match the results implies that $\mu_{r \text{ inc}}$ is approximately 12.5. This value can be obtained for a flux density greater than 1.8 Tesla, and at very low excitation. See Figure 13.21 of Attwood.³

This may explain the fact that the capacitive discharge test results and the bridge measurements agree reasonably well.

It must be understood, however, that the space harmonic distribution analysis that has been developed thus far does not consider the effect of the end turns on the calculated inductance per unit length. Therefore, the calculations made using this technique are most accurate for machines having many poles, where the length of the end turns are small compared to the active length of the rotor.

Self-Inductance Versus Radial Position

In these calculations it has been observed that the self inductance of the armature winding is approximately 65 percent greater than the self inductance of the compensating winding. This is due to the armature winding being in close proximity to the highly permeable laminated rotor ($H_g = 0$ at $r = R_r$), and also because the compensating winding is in close proximity to the solid steel pole tips ($H_r = 0$ at $r = R_s'$). To determine the sensitivity of the self inductance of the armature winding to the inner radius of the winding R_1 , the calculations were repeated over the range $R_r \leq R_1 \leq R_s$. The self inductance L_a of the armature winding is plotted versus R_1 in Figure 3-4. It can be seen from Figure 3-4 that the inductance varies from 80 μ H to 10 μ H as the inner winding radius is increased from the rotor periphery to the pole faces.

Effective Inductance vs Radial Separation of Windings

It is necessary to minimize the radial separation of the armature winding and compensating winding to improve the performance of the compensated pulsed alternator. For a given voltage, the energy delivered to a flashlamp load is strongly dependent upon the minimum circuit inductance. The pulse width is strongly dependent upon the flux compression ratio of the machine ($L_{\max}/L_{\min}$). Therefore, it is necessary to operate the device at the smallest air gap that mechanical and electrical constraints allow. The values $L_{\min}$, $L_{\max}$, and ($L_{\max}/L_{\min}$) are plotted as functions of radial separation ($R_5 - R_2$) in Figures 3-5, 3-6, and 3-7. As one might expect, the minimum inductance increases approximately linearly as the radial separation is increased. Unfortunately, the flux compression ratio decreases significantly as the radial

Figure 3-4. Armature Winding Self-Inductance versus Radial Position of Winding in the Air Gap

Figure 3-5. Minimum Effective Inductance versus Radial Separation of Armature Winding and Compensating Winding

Figure 3-6. Maximum Effective Inductance versus Radial Separation of Armature Winding and Compensating Winding

Figure 3-7. Flux Compression Ratio (L_{max}/L_{min}) versus Radial Separation of Armature Winding and Compression Winding

separation is increased. The maximum inductance decreases as the compensating winding inner radius R_5 is moved away from the ferromagnetic rotor boundary (R_2 is held constant).

One possibility to be investigated would be to maintain a six kilo-volt system as the machine dimensions are scaled upwards. This could be done by continuing to connect all lamps in parallel, rather than connect series pairs of 6 kV flashlamps in parallel for a 12 kV system. This would double the output current of the machine and would increase the difficulty of current collection and switching. Therefore, the tradeoff must be studied carefully. However, this would permit us to continue to use the prototype Compulsator test circuit configuration and maintain minimum conductor separation.

Effective Inductance vs Circumferential Span of Windings

The circumferential distribution of the windings is important in determining the effective inductance as the angular position of the rotor winding varies relative to the compensating winding during the pulse. The shape of the inductance variation is controlled by the relative magnitudes of the harmonic components of the armature and compensating winding self and mutual inductances. For cases where the conductors span a large fraction of the pole pitch, the second space harmonic is dominant (fundamental electrical component). As the width of the conductor span is reduced, the relative magnitude of the higher space harmonics are increased, especially the sixth space harmonic (third electrical harmonic). The calculated values of $L_{\min}$, $L_{\max}$, and $(L_{\max}/L_{\min})$ are plotted in Figures 3-8, 3-9, and 3-10. In all cases it is assumed that the radial thickness of the windings remain constant. Therefore, the circuit resistance must necessarily be increased as the conductor span is reduced.

As seen in Figure 3-8 the minimum inductance increases as the conductor span is reduced. The maximum inductance also increases as the span is decreased so that the flux compression ratio shown in Figure 3-9 is relatively flat. It can be seen that the prototype winding design ($\beta_{2C} = 70.2^\circ$) results in the maximum flux compression ratio. If the winding span were increased to say 82° , the minimum inductance would decrease $4\mu H$ with essentially no change in the ratio of maximum to minimum. However, this would require a smaller spacer width which would cause a much tighter bend radius on the inside conductor.

Since the resistance of the winding is increased as the span is reduced, it is important to consider the same variation of conductor span, but with a constant conductor cross-section. This will maintain the same winding resistance as the span is varied. Values of $L_{\min}$, $L_{\max}$, and $(L_{\max}/L_{\min})$ are plotted in Figures 3-11, 3-12, and 3-13 for the constant cross-section case. In these cases the radii R_2 and R_5 are held fixed, and the rotor and stator radii, R_4 and R_5 , are moved apart to accommodate the increased conductor thickness. Since the minimum inductance is increased and, as the conductor span is reduced, the flux compression ratio is decreased, a wide, radially thin conductor design is preferable.

Figure 3-8. Minimum Effective Inductance versus Conductor Span - Constant Radial Thickness

Figure 3-9. Maximum Effective Inductance versus Conductor Span - Constant Radial Thickness

$\frac{L_{MAX}}{L_{MIN}}$ VS SPAN
(CONSTANT THICKNESS)

Figure 3-10. Flux Compression Ratio (L_{max}/L_{min}) versus Conductor Span - Constant Radial Thickness

Figure 3-11. Minimum Effective Inductance versus Conductor Span - Constant Conductor Cross-Section

Figure 3-12. Maximum Effective Inductance versus Conductor Span - Constant Conductor Cross-Section

Figure 3-13. Flux Compression Ratio (L_{max}/L_{min}) versus Conductor Span - Constant Conductor Cross-Section

Based on the analysis performed to date, it appears that the space harmonic distribution technique for calculating the effective armature circuit inductance will prove to be a useful design tool. In addition to calculating inductance values for this configuration, we hope to modify the system equations to account for a ferromagnetic boundary condition ($H_\theta = 0$) at the stator bore. This would serve to model a machine with laminated poles. The laminated poles are interesting because of the need to increase the inductance variation while reducing the coupled eddy current loss. This is necessary to provide a current pulse of higher amplitude and reduced half width that is better suited to the task of efficiently driving a high energy flashlamp load for solid state laser amplifiers as well as other pulse power loads anticipated in the future.

References

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2. Attwood, S.S., Electric and Magnetic Fields, Dover Publications, Inc., New York, 1967, pp. 333-338.
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4. COMPENSATED PULSED ALTERNATOR - DESIGN AND MANUFACTURE

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4. COMPENSATED PULSED ALTERNATOR - DESIGN AND MANUFACTURE

Specifications

The engineering prototype compensated pulsed alternator is a vertical shaft single phase alternator with a rotating armature and a salient pole stator. Low reactance is obtained by compensating the armature flux with the flux of a stationary series winding, identical to the armature winding, but located in the stator bore. To minimize the reactance, both windings are radially thin and are wound in the air gap, thereby avoiding slot leakage reactance. The coupling between the armature winding and compensating winding varies with the angular position of the rotor, resulting in a time varying mutual inductance that tends to decrease the pulse width of the output current. A cutaway view of the prototype machine is shown in Figure 4-1.

Physical specifications of the machine are summarized in Table 4-1. Major components of the rotor are listed in Table 4-2. The component material, mass, and polar moment of inertia are given. Similarly, the major components of the stator are listed in Table 4-3. Again, the material and calculated mass are given for each component.

The total mass of the rotor, stator, and mounting torque frame is 9.8 metric tons (21,570 lbs). The overall dimensions of the machine are 1.58 m x 1.58 m x 1.83 m. At the maximum design speed of 5400 rpm, the inertial energy store of the 1200 kg rotor is approximately 3.4 megajoules. Since the moment of inertia of the armature conductors is only four percent of the total, essentially all of the discharge torque must be transmitted across the ground plane insulation which bonds the stranded Litz wire conductors to the rotor.

The reaction torque developed in the compensating winding must similarly be transmitted through its ground plane insulation to the bore of the 7400 kg stator. Therefore, the shear strength of the bond formed by the ground plane insulation to the rotor periphery and to the stator bore is critical to the design of the machine.

Ultimately, the reaction torque must be transmitted to ground. If the stator were held stationary, the reaction force would be difficult to transmit because of the high loading required on the mounting bolts.

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Figure 4-1. Cutaway View of UT-CEM Prototype Compensated Pulsed Alternator

TABLE 4-1
PHYSICAL SPECIFICATIONS OF ENGINEERING PROTOTYPE COMPULSATOR

<u>Rotor</u>		
Maximum diameter (m)	0.401	(15.79 in)
Length at maximum diameter (m)	1.216	(47.86 in)
Slip ring spacing, ϕ to ϕ (m)	1.279	(50.36 in)
Journal bearing spacing, ϕ to ϕ (m)	1.543	(60.74 in)
Journal diameter (m)	0.152	(6.00 in)
Total shaft length (m)	2.016	(79.38 in)
Polar moment of inertia (J-sec ²)	21	
Maximum speed (rpm)	5400	
Inertial energy stored at 5400 rpm (MJ)	3.36	
Total mass (kg)	1203	(2656 lb)
<u>Stator</u>		
Width x Breadth (m)	0.996 x 0.996	(39.2 in x 39.2 in)
Length of backiron (m)	0.826	(32.5 in)
Inner bore diameter (m)	0.423	(16.64 in)
Total mass (kg)	7388	(16,306 lb)
<u>Torque Frame</u>		
Height (m)	1.829	(72 in)
Width x Breadth (m)	1.575 x 1.575	(62 in x 62 in)
Total mass (kg)	1183	(2610 lb)

TABLE 4-2
PROTOTYPE COMPULSATOR ROTOR COMPONENTS

Component	Material	Calculated Mass (kg)	Calculated Polar Moment of Inertia (J-sec ²)
Laminations	29 Gauge M-19 sheet steel	8.934×10^2	1.726×10^1
Washer spacers	Steel	8.459×10^0	2.688×10^{-1}
Belleville washers	Ti-6Al-4V	5.709×10^1	1.106×10^0
Shaft	4340 steel (R _c 35)	1.119×10^2	1.288×10^{-1}
Shaft nuts	4340 steel	3.852×10^1	1.452×10^{-1}
Bearing journals	4140 steel	1.595×10^1	8.578×10^{-2}
Litz wire conductors	12x7x30 AWG copper	2.229×10^1	8.658×10^{-1}
Turn to turn insulation	Hexcel F159/120	2.854×10^0	2.016×10^{-1}
Ground plane insulation	Hexcel F155/7781	9.088×10^0	3.482×10^{-1}
Fiberglass banding	G.E. No. 76843	2.945×10^0	1.409×10^{-1}
Output terminal plates	ETP 110 copper	4.514×10^0	1.109×10^{-1}
Output terminal insulation	G-10	1.845×10^0	4.587×10^{-2}
Slip rings	ETP 110 copper	1.089×10^1	1.071×10^{-1}
Slip ring insulation	G-10	2.044×10^0	2.035×10^{-2}
Slip ring nuts	Steel	1.699×10^0	1.757×10^{-2}
Brush dust rings	G-10	9.927×10^{-1}	1.405×10^{-2}
Thrust bearing runner and nut	Steel	4.136×10^0	1.126×10^{-2}
Balancing rings	Steel	7.992×10^0	5.156×10^{-2}
Slinger ring	Steel	6.501×10^0	9.014×10^{-2}
TOTAL		1.203×10^3	2.102×10^1

TABLE 4-3
PROTOTYPE COMPULSATOR STATOR COMPONENTS

Component	Material	Calculated Mass (kg)
Back iron	Hot rolled steel	2.245×10^3
Trapezoidal pole mounts	Hot rolled steel	1.489×10^3
Poles	Hot rolled steel	1.069×10^3
Pole supports	304 stainless steel	3.180×10^2
Torque frame mounting flanges	Steel	3.991×10^2
Field coil conductor	102 OFHC copper	4.501×10^2
Field coil insulation	GE No. 76593 (Fusa-Flex)	1.654×10^1
Bearing support strut mounting flanges	Steel (included)	3.611×10^2
End turn support rings	304 stainless steel	1.694×10^2
End turn hoop ring	304 stainless steel	5.754×10^1
Litz wire conductors	12x7x30 AWG copper	2.220×10^1
Turn to turn insulation	Hexcel F159/120	2.854×10^0
Ground plane insulation	Hexcel F159/7781	9.877×10^0
<u>Subtotal - main stator assembly</u>	(14,066 lb)	6.373×10^3
Brush assemblies (based on measured weight)	G-10, Morganite CMIS ETP 110 copper (129 lb)	5.844×10^1
Bearing support struts	304 stainless steel	3.547×10^2
Support strut mounting shims	Steel	1.527×10^2
Support strut mounting flanges	Steel	2.537×10^1
Journal bearing housings with covers	Steel	3.439×10^2
Thrust bearing housing with covers	Steel	3.987×10^1
Seal assembly	Aluminum; steel	3.987×10^1
<u>Subtotal - bearing housings and supports</u>	(2,111 lb)	9.564×10^2
<u>Total stator</u>	(16,306 lb)	7.388×10^3

The load on the mounting bolts is reduced markedly by a compliant mounting assembly which supports the stator axially and radially, but allows the stator to rotate. Since the inertia of the stator is large compared to that of the rotor, it rotates at a low frequency and does not alter the shape of the delivered output pulse significantly. The maximum displacement of the stator is approximately 6.4 mm. The mass of the stator torque frame assembly is 1180 kg.

The rotor is located using conventional tilting pad hydrodynamic thrust and journal bearings. A high pressure hydrostatic lift supply provides load capacity for starting and low speed operation. The bearing housings are supported by stainless steel struts which are attached to mounting flanges welded to the stator iron.

The main field excitation is provided by water cooled copper coils, wound in three double pancakes around each pole. The field mmf is designed to provide a peak magnetic flux density of 1.9 to 2.0 Tesla in the airgap.

The armature current is collected at copper slip rings located at each end of the rotor. Sintered copper graphite brushes, identical to those used on UT-CEM high current homopolar generators, are employed on the prototype compulsator. The brushes are loaded against the slip ring pneumatically. The brushes are normally held away from the slip ring surface by springs to reduce frictional heating and wear.

The design of the engineering prototype compensated pulsed alternator is based on a series of electrical and mechanical tradeoffs. However, performance is generally maximized if the rotor tip speed is maximized, applied field strength is maximized, and the mechanical air gap is minimized. To extend the technology of rotating machinery for pulsed power applications, several major design features are included in the prototype compulsator. These include the following:

1. Air gap armature and compensating windings
2. High speed laminated rotor
3. High shear strength ground plane insulation system

The design and manufacture of these and other major machine components are discussed in the following sections.

Applied Field Magnetic Circuit

The applied field magnetic circuit consists of the laminated rotor, solid steel poles, and trapezoidal and rectangular steel plates which comprise the fabricated stator. The poles are joined mechanically at the stator bore by stainless steel bolts and stainless steel bars, which provide mechanical strength, yet retain a high reluctance to leakage flux. Each pole is excited by a 48 turn water cooled copper coil, wound in three double pancakes. All four coil sets (one for each pole) are connected electrically in series. A top cross-sectional view of the magnetic circuit is shown in Figure 4-2.

Field Excitation Requirements

To the first approximation, the required ampere turns per pole is calculated using Equation 4-1.

$$N_f i_f = \frac{1.9}{\mu_0} \left[g + \frac{h_p}{\mu_{rp}} + \frac{\ell_r}{\mu_{rr}} \right] + \frac{1.6\ell_s}{\mu_0\mu_{rs}} \quad (4-1)$$

where g is the magnetic air gap, h_p is the radial height of the pole, ℓ_r and ℓ_s are the effective path lengths of the rotor and stator flux (per pole), μ_0 is the permeability of free space and μ_{rp} , μ_{rr} , and μ_{rs} are the relative permeabilities of the pole, rotor and stator material. It is assumed that the average flux density in the poles and rotor is 1.9 Tesla, and the average flux density in the stator is 1.6 Tesla. Estimated values of these parameters are given in Table 4-4.

TABLE 4-4
APPLIED MAGNETIC FIELD CIRCUIT PARAMETERS

g (m)	2.08×10^{-2}
h_p (m)	1.40×10^{-1}
ℓ_r (m)	1.07×10^{-1}
ℓ_s (m)	3.30×10^{-1}
μ_{rp} @ 1.9T	7.83×10^1
μ_{rr} @ 1.9T	$> 7.83 \times 10^1$
μ_{rs} @ 1.6T	2.60×10^2

Figure 4-2. TOP VIEW CROSS-SECTION OF COMPULSATOR
APPLIED FIELD MAGNETIC CIRCUIT

WLB
UT-CEM
LLL
1-18-80

Using the values given in Table 4-4, the ampere turns required per pole is only 3.78×10^4 (A-t). However, we realize that the non-linearity of the ferromagnetic material, especially at high levels of induction, can lead to higher ampere turns than that predicted by Equation 4-1. Therefore a two dimensional flux plot was made for the "as-constructed" configuration. To reduce computation costs only one-eighth of the machine was modeled, taking advantage of the symmetry of the problem. The selected element grid used with the UT finite element flux mapping program TEXMAP is shown in Figure 4-3. Using this program, it was determined that 4.76×10^4 A-t per pole were required to generate a peak magnetic flux density of 1.9 Tesla in the air gap at the average radius of the armature conductors. The resulting flux plot is shown in Figure 4-4. The increased mmf is required to overcome the effects of the high leakage flux (26 percent) and local saturation. Therefore, the minimum field excitation current is approximately 1000 amps.

The calculated radial magnetic flux density is plotted as a function of mechanical angle in Figure 4-5. The radial field B_r can be represented by a Fourier series of the form

$$B_r(\theta_m) = \sum_{n=1}^{\infty} B_{rn} \cos(nN_p\theta_m/2) \quad (4-2)$$

where θ_m is the mechanical angle, and N_p is the number of poles. The maximum flux density $B_{\max} = B_r(\theta_m = 0)$ is 1.9 Tesla. The relative values of B_n to the maximum flux density $B_{\max}$ are listed in Table 4-5.

TABLE 4-5
FOURIER COEFFICIENTS ($B_{rn}/B_{\max}$)

n	$\alpha_n = B_n/B_{\max}$
1	1.12
3	-0.18
5	0.07
7	-0.02
9	0.01

These coefficients are useful for voltage and power loss calculations.

ELEMENTS

Figure 4-3. Finite Element Grid Map

POTENTIAL

R. B.
UT - GEN
LLS
1-21-60

Figure 4-4. Magnetic Potential Lines - $N_f i_f = 4.76 \times 10^4$ A-t
Per Pole

BIRD
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Figure 4-5. Radial Magnetic Flux Density from
Finite Element Method Flux Plot

After fabrication was completed, the applied radial magnetic field was measured using a F.W. Bell BHT-900 JB 807 Hall generator, which was mounted on the end of a thin (1.6 mm) strip of G-10 insulation. The probe was then placed at the center of a pole in the air gap. The radial component of the applied field is plotted versus field current in Figure 4-6. As can be seen, the measured magnetic flux density at 1000 amps is just slightly greater than 1.9 Tesla. This agrees very well with the value calculated with the 2-D flux mapping code.

The coils are designed to operate continuously at 1250 amps to provide flexibility in operation and testing of the prototype, and to provide additional margin. This margin is needed to insure that the voltage specification is met, especially since the effect of end leakage and drilled holes could not be modeled with the two dimensional computer program. This safety margin proved to be necessary because of end effects. The presence of axial leakage flux at the ends of the poles increases the total flux, and therefore causes a higher level of saturation of the pole material. Therefore, the useful radial field is reduced near the ends of the poles. The measured radial flux density is plotted versus axial position in Figure 4-7.

Figure 4-6. Prototype Compulsator Applied Field Magnetization Curve

Figure 4-7. Radial Flux Density Versus Axial Position of Hall Generator in Air Gap

Field Coil Design and Fabrication

The field coils are constructed of 1.2 cm square copper conductor with a 0.68 cm round coolant passage. The conductor was supplied by LLL in 18.3 m (60') putups. A photograph of the conductor is shown in Figure 4-8. Each field coil consists of three sixteen turn double pancake coils connected electrically in series and hydraulically in parallel.

North polarity coils have double pancake coils with crossed-open-crossed terminals. South polarity coils have open-crossed-open terminals. The coils are terminated in this fashion to facilitate the series electrical connections. The return (positive) lead is brought around the machine as shown in Figure 4-9 to eliminate a completed turn which would generate axial leakage fields in the brush and bearing regions. A completed field coil with the original output terminations is shown in Figure 4-10. The terminals were later revised to provide additional clearance between the coils and the armature brush ring assembly.

Figure 4-8. Hollow Copper Conductor for Field Coil Construction (1.2cm x 1.2 cm with 0.68cm round cooling passage)

Figure 4-9. TOP VIEW OF COMPULSATOR APPLIED FIELD CONNECTION

Figure 4-10. Completed Field Coil Assembly

The hollow copper conductor is insulated with a half lap wrap of 0.18 mm (7 mil) sealable armor tape (General Electric Fusa-Flex No. 76593). The conductors were wound in a double pancake on a rectangular wooden form. The tape was easily damaged if mishandled, and it was necessary to form the first few turns bare, lift the conductors off of the mandrel, and then wrap with the insulating tape. The outer layers had a larger minimum bend radius at the coil ends and could be formed after wrapping without damaging the insulating tape.

Two splices were required per double pancake coil since the total length per double pancake (41.2 m) was more than two times the length of a put up. The splice locations were made so that the splices did not appear at the crossover from the bottom layer to the top layer, or on a corner bend.

Each splice was made using the ferrule method. The round cooling passages were reamed out, and a copper ferrule was silver soldered into the reamed holes. All splices were tested by pressurizing the conductor with freon at 150 psig and observing the color of a flame held at the joint to detect any leakage.

After the three double pancake coils were wound, the wooden forms were removed from the centers of coils. The coils were then stacked with 0.5 mm (20 mil) layers of Nomex insulating paperboard between each double pancake coil. Similar layers were also inserted between layers of the double pancake winding. The Nomex insulation was used to reduce the chances of dielectric breakdown at the inner radius of each bend at the sharp corners caused by keystoning of the conductor.

The three layers were finally wrapped together with a half lap wrap of the same B stage epoxy insulating tape. The coil assembly was then clamped in an aluminum form and wrapped with thermal insulation. The B staged epoxy was cured by flowing steam through the coolant passages. This curing method provided a uniform temperature distribution throughout the coil.

All field coils were tested at 1200A under steady state conditions at a coolant flow of 0.19 ℓ /sec (3 gpm). The measured voltage drop and calculated resistance of each coil are listed in Table 4-6.

TABLE 4-6
MEASURED FIELD COIL RESISTANCE

<u>Test</u>	<u>Voltage Drop (volts) at 1200A</u>	<u>Resistance ($m\Omega$)</u>
North coil #1	26.0	21.7
South coil #1	25.3	21.1
North coil #2	25.7	21.4
South coil #2	25.7	21.4

The measured inductance of each coil in air at 1 kHz is 1.9 mH. The pressure drop at 0.19 ℓ /sec is 0.34 MPa (50 psi).

Mechanical Design of the Stator

The stator assembly is designed to withstand the forces generated by a 150 kiloamp short circuit for the original estimated inductance variation. The discharge torque is considered to be the sum of two components, one due to alternator JxB forces, the second due to the flux compression action.

$$T_m = -V_{ac} i_g / \omega_m + 1/2 i_g^2 \frac{dL_g}{d\theta_m} \quad (\text{N-m}) \quad (4-3)$$

V_{ac} is the open circuit alternator armature voltage, i_g is alternator current, ω_m is the angular velocity of the rotor, and L_g is the effective armature circuit inductance. For the mechanical design, the peak deceleration torque is

$$T_{max} = -1.28 \times 10^6 - 1.42 \times 10^6 = -2.7 \times 10^6 \text{ N-m } (1.99 \times 10^6 \text{ ft-lb})$$

The average internal pressure is set equal to $B_\theta^2 / 2\mu_0$, where

$$B_\theta = \mu_0 H_\theta = \mu_0 k_z = \mu_0 \frac{n i_{gen}}{\tau} \quad (4-4)$$

where n is the number of conductors per pole and τ is the polar pitch.

For $n = 12$, $i_{gen} = 150 \text{ kA}$, and $\tau = 0.332 \text{ m}$, the average tangential field B_θ is 6.8 Tesla. For design purposes, an internal pressure of 20.7 MPa (3000 psi is assumed).

The ideal geometry for sustaining these forces would be a cylindrical cast steel vessel. However, the lead time of such a structure was too long to fit into the project schedule. Therefore, the stator was fabricated from hot rolled steel plate (A-36). A cross-sectional view of the stator assembly is shown in Figure 4-11. As will be discussed later, the fabricated assembly proved useful in applying pressure during curing of the pre-preg epoxy resin fiber glass tape system used for the ground plane insulation.

DISCHARGE FORCES (FAULT MODE, 5400 RPM, 150 KA)
 T = PEAK DISCHARGE TORQUE = 2.7×10^6 N-M
 P_m = PEAK MAGNETIC PRESSURE = 20.7 MPa (3000 psi)

Figure 4-11. Fabricated Stator Assembly

The mechanical loads placed on a pole assembly are illustrated in Figure 4-12. Stainless steel blocks (11.3 cm x 11.3 cm) are bolted to the corners of each pole with eight 2.54 cm (1 in.) stainless steel bolts to form a thick walled structure. This structure must sustain the magnetic pressure without any deflection that could cause a crack in the epoxy resin bond. The bolts are loaded in tension by the magnetic pressure over the projected area of the stainless block.

The poles are attached to the trapezoidal-shaped steel plate with six 2.0 - 4 1/2 UNC bolts. In addition, two rows of five 5.08 cm (2 in) dowel pins are inserted 5.08 cm (2 in) deep into the poles.

The torque reaction places a moment on the poles which loads the six bolts in tension. This tensile load is approximately 134 MPa (19.4 ksi). Neglecting friction, the dowel pins must transmit the reaction torque to the back iron. The shear stress on the dowels is 158 MPa (22.9 ksi). The dowel bearing stress is 124 MPa (18.0 ksi). The magnetic pressure is ultimately taken in tension in the large rectangular steel plates. The key bearing stress is 123 MPa (17.9 ksi) and the key shear stress is 46.5 MPa (6.75 ksi).

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DISCHARGE FORCES: (FAULT MODE, 5400 rpm, 150 KA)

T = Peak Discharge Torque = 2.7×10^6 N-M

P_m = Peak Magnetic Pressure = 20.7 MPa (3000 psi)

SIGNIFICANT STRESSES:

S_1 = Tensile Bolt Stress = 191 MPa (27.7 KSI)

S_2 = Dowel Bearing Stress = -124 MPa (18.0 KSI)

Dowel Shear Stress = 153 MPa (22.9 KSI)

S_3 = Key Bearing Stress = 123 MPa (17.9 KSI)

S_4 = Key Shear Stress = 46.5 MPa (6.75 KSI)

Figure 4-12. Stator Forces and Calculated Stresses

A side view of the completed stator assembly is shown in Figure 4-13. The steel mounting flanges are welded to the rectangular back iron pieces. Steel box beam is bolted between the mounting flanges to be attached to the torque frame. Clearance holes are drilled in the flanges for bolts to clamp the bearing support struts. Reamed holes are provided for dowel pins to locate and align the bearing support struts.

Torque Frame Design

The torque frame is a structure designed to support the machine axially and radially, but allow a slight rotation of the back iron during a discharge. The torque frame is constructed of eight I beams (6 I 12.5), two per corner, which are connected at the top and bottom by a square frame of rectangular tubing. The torque frame and stator are shown during construction in Figure 4-14.

Figure 4-13. Side View of Stator Assembly

Figure 4-14. Torque Frame Assembly During Construction

By allowing the stator to rotate, the total peak load transferred to the torque frame as a result of the reaction torque is reduced from $6.82 \times 10^6 \text{ N}$ ($1.53 \times 10^6 \text{ lb}$) to $1.15 \times 10^5 \text{ N}$ ($2.56 \times 10^4 \text{ lb}$). As the stator rotates, it compresses Belleville washers against the I beams which form the structure of the torque frame. The springs are essentially serving as force attenuators. There are two sets of Belleville washers being compressed at each of the four corners of the torque frame located at a radius of 79.4 cm (31.3 in.) from the center line of the machine. The back iron is allowed to rotate 7.33×10^{-3} radians, compressing the Belleville springs 0.532 cm (0.23 in.). The stator is supported axially by 16 0.32 cm (0.125 in.) thick, 13 cm (5 in.) wide aluminum plates, four per pole. These aluminum plates sustain the stator rotation in bending. A detailed view of the compliant Belleville washer/mounting assembly is shown in Figure 4-15.

Stator motion was recorded during the high speed discharges by a simple pen chart recorder. Three test run charts are shown in Figure 4-16, Run No. 119 (4200 rpm), Run No. 121 (4800 rpm) and Run No. 122 (5400 rpm - fault). Notice that the recorded motion was approximately 0.25 cm for the normal discharges, and 0.8 cm for the discharge under fault.

Figure 4-15. Detailed View of Compliant Belleville Washer/Mounting Assembly

There are two critical frequencies within the operating range of the compulsator with the torque frame compliant mounting system. The aluminum plates which support the stator allow the stator to rock back and forth (up and down) when these critical frequencies are excited. These are excited by the alternator at low speed, and by the dc motor drive at higher speeds. Additional damping should be provided to reduce the amplitude of this vibration mode when the rotor is accelerated from rest to full speed. (See Rotor-Bearing-Support System Dynamics).

Mechanical Design of the Rotor

In order to reduce eddy current losses, the rotor is made of 2913 steel (M-19) laminations, 0.036 cm (0.014 in.) thick, 38.1 cm (15 in.) in diameter and shrunk fit on to a 9.65 cm (3.8 in.) diameter AISI 4340 steel shaft heat treated to R_c 34. Because the stiffness of the shaft is insufficient to keep the first rotor critical above the maximum operating speed of 5400 rpm, it is necessary to compress the laminations in order to increase the effective flexural modulus of the rotor, hence the rotor stiffness. The rotor

Figure 4-16. Rotation of Stator in Compliant Belleville Washer Assembly

cannot be allowed to pass through a critical because hysteretic losses from sliding of the lamination interfaces would result in a rotor instability.¹ The lamination preload is applied with two (one per end) large, titanium (Ti-6Al-4V) Belleville washers, 6.03 cm (2.375 in.) thick and 38.1 cm (15 in.) in diameter. Because of the Belleville washer configuration, the preload of 2.67×10^6 N (600,000 lb) preferentially loads the outside diameter of the laminations although the washer is flattened to partially load the inner diameter also.

The required preload was not arbitrarily selected, but resulted from a series of tests performed on sample stacks of laminations. The two guiding design criteria were the interlaminar resistance and effective modulus versus load. With increasing load, the effective flexural modulus of the stack of laminations increases as the interlaminar resistance decreases.

The desired lamination core plating, C-5, could not be obtained on the schedule required for construction of the prototype. The plating received, C-0, was unacceptable and required that an interlaminar insulation be applied that could withstand the high loads. The first type of insulation tested was Sterling U-87/PS, air dry varnish.

Figure 4-17 shows the measured overall resistance versus load of two separate stacks of laminations. The bottom curve is the test results of loading a stack of 1000 varnished laminations. Even as the load was held steady, the resistance continued to drop and at 1.33×10^6 N (300,000 lb), measured 182 ohms. This was unacceptable since a value of 782 ohms was desired in order to keep the eddy current losses at an acceptable level.

The next insulation tested and finally used was a military refinish concentrate made by Atlanta Cutlery. It is a chemical conversion coating which phosphatizes the surface. The varnish previously applied was baked off the laminations before the chemical conversion coating was applied. The resistance versus load of a stack of 500 coated laminations is also shown in Figure 4-17 and at 3.11×10^6 N (700,000 lb) measured 9,040 ohms, over an order of magnitude above the design goal of 391 ohms. Note that even as the load was fluctuated, the resistance remained relatively stable and repeatable. The resistance did register lower after the load was

Figure 4-17. Interlaminar Insulation Resistance vs Load

decreased than when the load was initially applied. This is probably due to an increasing number of small asperities breaking through the insulation as the load is increased and then remaining in contact with the adjacent lamination as the load is decreased.

During the same test with the chemical conversion coated laminations, the amount of axial compression versus load was measured and is shown in Figure 4-18a. When the stack is initially compressed, the total deflection is significant. This is a result of the air being squeezed out from between each lamination, small asperities being flattened, and any warpage in the laminations being flattened. When the same stack is then compressed a second time, even after remaining unloaded for one day, the total deflection is considerably less. An interesting fact is that the slope of the curve as the load is decreased is the same as that for increasing load indicating that after the stack is loaded, its mechanical characteristics are repeatable.

Figure 4-18b is an enlarged section of the first loading curve and shows some other interesting facts. First note that as the stack is brought

Figure 4-18b

Figure 4-18a

Figure 4-18. Lamination Deflection vs Load

to full load, at two points the load is reduced and the stack still shows an increase in deflection. This was noticed in other tests not presented here and is related to the amount of time the stack is allowed to sit at load before the deflection measurement is made. If the load is held steady, the stack will continue to compress for many minutes. It is suspected this time element is a result of the air being squeezed out from between the laminations. Another important fact is that after the stack stops creeping and the load is fluctuated, the stack does not load and unload along the same curve, indicating some hysteresis in the stack.

Since the resistance remained high for all the loads tested, the level of load to be used is determined by mechanical limitations. The Belleville washers, which apply the preload, are held in place with two large nuts which also serve as the bearing journals. These nuts were tightened using a stud tensioner loaned to CEM by the DuPont chemical processing plant in Victoria, Texas. The device works by stretching the stud (in our case, the shaft) and then "hand tightening" the nut down against the Belleville washer. The device has a $3.56 \times 10^6 \text{ N}$ (800,000 lb) pulling capacity which produces the maximum stresses the modified 60° stub-tooth Acme threads and shaft can take. Due to relaxation in the threads as the load is transferred from the stud tensioner to the nut, the resulting preload was less although the minimum desired is $2.67 \times 10^6 \text{ N}$ (600,000 lb).

Clamped up sections of laminations 22.9 cm (9 in.) long were bored with a taper of $6.86 \times 10^{-3} \text{ cm}$ (0.0027 in.) on the diameter and the shaft then ground to match. After the application of the lamination insulation, the inner bore of the laminations were aligned for the shrink fit by pulling one lamination at a time up against two small ground shafts glued together and inserted down the bore. The entire stack was then clamped as tightly as possible without affecting the bore alignment. The alignment was checked by lowering the shaft in at room temperature until the tapers matched. The final shrink fit was done by chilling the shaft in liquid nitrogen and then dropping it into the laminations. Figure 4-19 is a photograph of the rotor with the Belleville washers and nuts in place.

The inverse of the slope of the curve in Figure 4-18a can be considered an effective Young's Modulus, E_{eff} , of the stack of laminations in compression. Using the data obtained on the final unloading in Figure 4-18b, $E_{\text{eff}} = 1.88 \times 10^{10} \text{ MPa}$ ($2.72 \times 10^6 \text{ psi}$). Although what is actually desired is the flexural modulus, it could not be measured and the above E_{eff} was determined to be sufficient for the dynamic calculations.

Figure 4-19. Laminated Rotor Assembly

Rotor-Bearing-Support System Dynamics

A discrete, lumped mass model of the rotor-bearing-support system (see Figure 4-20) was performed using a CDC 6600 computer to solve for the complex eigenvectors and complex eigenvalues. The torque frame, bearing supports, bearings (including damping), and rotor are included in the model. The first rotor critical was calculated to be 621 rad/sec, 10% above maximum operating speed.

Two lower frequency criticals were found to occur at 152 rad/sec and 188 rad/sec. The lower critical frequency is associated with the stator in a transverse or rocking mode in the torque frame. The second critical frequency is associated with the stator in a rotational mode in the torque frame.

K_{TF} = TORQUE FRAME STIFFNESS = 6.04×10^7 N/M
 K_{BS} = BEARING SUPPORT STIFFNESS = 1.29×10^9 N/M
 K_B = BEARING STIFFNESS = VARIED
 C_B = BEARING DAMPING = VARIED
 E_{EFF} = EFFECTIVE YOUNG'S MODULUS OF ROTOR = VARIED
 K_R = ROTOR STIFFNESS; INCLUDES SHEAR DEFLECTION;
 DEPENDENT UPON E_{EFF} = VARIED
 K_{mag} = MAGNETIC SPRING STIFFNESS = 3.97×10^7 N/M
 M_R = MASS OF ROTOR = 1.09×10^3 KG
 M_{BI} = MASS OF BACK IRON = 9.07×10^3 KG
 M_B = MASS OF BEARING HOUSING = 230 KG

Figure 4-20. LUMPED MASS DYNAMIC MODEL - for Vibration Analysis

In this calculation the effective torque frame stiffness in the transverse mode is 6.04×10^7 N/m (3.45×10^5 lb/in.). The effective stiffness per end was calculated using the following assumptions:

1. On one corner the four aluminum plates are loaded radially with each plate having a stiffness of 1.98×10^7 N/m (1.13×10^5 lb/in.). The plates are in series with two I-beams, loaded in the soft direction. The stiffness of each beam is 4.48×10^7 N/m (2.56×10^5 lb/in.).
2. On the adjacent corner, the four aluminum plates are loaded in bending in the transverse (soft) direction. The aluminum plate stiffness is small compared to that of the Belleville

washer stacks (9.84×10^6 N/m per stack, 5.63×10^3 lb/in.).

Two washer stacks are in series with two I beams, loaded in the stiff direction (1.36×10^8 N/m, 7.8×10^5 lb/in.).

3. The effective spring constant per end is the sum of the spring constants described above. $K_{TF} = 4.2 \times 10^7 + 1.84 \times 10^7 = 6.04 \times 10^7$ N/m.

If the I-beams were made infinitely stiff, the first critical frequency would only be a factor of two higher, still considerably lower than the maximum operating speed (frequency) of the machine. Simply stated, the torque frame is made soft so that the stator can rotate to reduce the load transferred to the foundation. However, this also results in a soft stator transverse mode.

Therefore, it is recommended that damping be added to the torque frame assembly, rather than to attempt to raise the first critical frequency. Some damping has been added by loading bolts in compression between the I-beams and the bearing housing support struts, which are welded to the stator. High hysteresis vibration mounts may also be incorporated into the mounting system to provide additional damping.

Rotor Critical Frequency Measurement

The first rotor critical frequency was measured using a Hewlett-Packard 5420A Fast Fourier Transform digital signal analyzer. The technique used was to measure the transfer function of rotor mid-point acceleration to an impulse. The rotor was placed between two structural steel supports filled with concrete and bolted to the laboratory floor in a bed of grouting. The rotor was then struck with a rubber-faced hammer near an accelerometer placed at the center of the rotor. A second accelerometer was mounted on the opposite side of the head of the hammer to measure the applied impulse. The transfer function based on the average of ten impulses is shown in Figure 14-21.

The first rotor critical frequency measured in this manner is 97.01 Hz or 5821 rpm. As a second check of this measurement, a 133 N (30 lb) electromechanical shaker was clamped at mid rotor and was tuned to resonance by varying the frequency of the power amplifier driving signal. The maximum amplitude of the accelerometer signal occurred at 98 Hz (5880 rpm).

Figure 4-21. Measured Rotor Impulse Response.

These measurements compare reasonably well to the predicted value of 98.8 Hz. There is some uncertainty to the actual magnitude of the applied preload due to relaxation of the stack after the hydraulic load was removed.

Bearings

The prototype machine is equipped with hydrodynamic tilting pad bearings manufactured by Kingsbury, Inc. at Philadelphia, PA. These bearings are self-aligning in both the axial and circumferential directions, requiring less stringent and less expensive manufacturing tolerances. These bearings have damping and squeeze film protection from overloads, and are generally free from whirl instabilities.

Thrust Bearing

The thrust bearing is Kingsbury Type JHJ-6, a six inch diameter two-sided bearing with six shoes per side. The thrust bearing and thrust bearing runner are shown in Figure 4-22.

Figure 4-22. Kingsbury Type JHJ-6 Thrust Bearing and Runner

The shoes are pivot mounted for self-alignment. The thrust load is evenly distributed or equalized by interlocking leveling plates, which are held in place by the base ring, which also holds the shoes against rotation. The rated load capacity of the bearing is $2.49 \times 10^4 \text{ N}$ (5600 lb) at 400 rpm and $2.66 \times 10^4 \text{ N}$ (6000 lb) at 6000 rpm. At a minimum speed of 400 rpm, the load capacity is $1.71 \times 10^4 \text{ N}$ (3.850 lb).² Below this minimum speed, it is necessary to support the rotor with a hydrostatic lift pad. The hydrostatic lift pad is mounted at the end of the shaft. When the hydrostatic lift pocket is pressurized at 5.5 MPa (800 psi), the rotor is raised approximately 0.025 mm (one mil).

The critical manufacturing tolerance in the thrust bearing assembly is the run out of the thrust bearing runner. The faces of the thrust bearing runner must be perpendicular to the shaft within 0.0025 mm (0.1 mils) to obtain proper hydrodynamic operation.

The bearings are supplied with a load cell, one resistance temperature probe (RTD) on the loaded side, and one RTD on the unloaded side. The bearings are designed to operate with an inlet oil temperature of 52° C (125° F) using oil with 150 saybolt Universal Viscosity at 38° C (100° F). The oil flow rate is sized to limit the outlet oil temperature to 65° C (150° F).

Journal Bearings

The journal bearings are of the horizontal pivoted shoe type. The bearing surface is divided into five sections (shoes), which are enclosed in a split aligning ring and are supported within the ring on a spherically shaped pivot. The spherical pivot or button, allows operation with some axial misalignment and tilt of the journal. The pads of the six inch diameter bearing are 9.2 cm (3 5/8 inches) long (non-standard) to provide additional load capacity without increasing the journal radius. One journal bearing is shown in Figure 4-23.

Figure 4-23. Hydrodynamic Tilting Pad Journal Bearing

A standard six inch journal bearing with an effective pad length of 6.35 cm (2.5 inches) has a maximum load capacity of $1.69 \times 10^4 \text{ N}$ (3800 lb) at 5400 rpm.³ Therefore, the load capacity of the prototype compulsator journal bearing is 45 percent higher or $2.45 \times 10^4 \text{ N}$ (5500 lb).

The outer diameter of the shaft nuts, which are tightened on the threaded shaft to clamp the lamination stack, also serve as the bearing journals. Hardened steel sleeves, shrunk fit onto the shafts nuts and

ground concentric with the shaft centers, serve as the actual bearing surfaces.

One question that was raised during the design of the engineering prototype and the selection of the journal bearings, was the effect of light loading on bearing stiffness and the transition between subcritical and supercritical operation.

We have not experienced any problems with operating hydrodynamic journal bearings in the vertical shaft configuration of this machine. The bearings are heavily damped, and the effect of a subcritical to supercritical transition have not been noticeable. We have recorded shaft displacement and bearing housing acceleration, and have no evidence of bearing criticals. We have observed acceleration of the bearing housing due to vibration of the stator assembly in the compliant mounting assembly, especially when the lower critical frequencies of the system are excited by the dc motor drive. We propose to address this problem by adding radial damping to the compliant mounting assembly.

Bearing Housings

The Kingsbury journal bearings are press fit and keyed into circular steel housings which serve to locate the bearings and distribute the oil lubricant to the tilting pads. Each bearing housing is supported by four 5.1 cm x 15.2 cm (2 in. x 6 in.) stainless steel struts, which are flange mounted to the stator assembly. The struts are fabricated of non-ferrous material so that the bearing support system does not provide a low magnetic reluctance path in parallel with the main air gap. To increase the mechanical stiffness of the support system, 2.5 cm x 10 cm (1 in. x 4 in.) stainless bars are welded to the struts to form a T section.

The thrust bearing housing is bolted to the lower journal bearing housing, so that the weight of the rotor is supported by the lower four stainless steel struts. The bearing housings are designed to accommodate an oil flow of approximately 0.31 ℓ /sec (5 gpm) for each journal bearing and the thrust bearing.

Lower Seal (Upper Journal Bearing)

The most important oil seal of the machine is the lower seal of the upper journal bearing housing. The bottom of the thrust bearing housing is not penetrated by the shaft, and gravity works to prevent leakage of the top seals on both the upper and lower journal bearing housings.

A top view of the lower seal assembly is shown in Figure 4-24.

Figure 4-24. Lower Seal Assembly - Top Journal Bearing

A steel slinger ring is used to pump the oil radially outwards toward the scavenge ring. Four non-contacting scrapers are located at 90 degree intervals to direct the oil into the four individual scavenge sumps. A dam and reservoir beneath the slinger is used to collect any oil that is not diverted to the main scavenge lines. This reservoir is scavenged separately by a secondary pump.

Armature Winding Design and Fabrication

The peak pulsed power rating of the compensated pulsed alternator is limited by armature circuit reactance. In an effort to minimize the machine

reactance, a compensating winding is connected in series with the rotor armature winding. To further reduce the alternator reactance, the armature and compensating conductors are not imbedded in slots, but are held in place by the adhesive bond formed by the ground plane insulation. By locating the conductors in the magnetic air gap, the effective radial separation between the windings is reduced to the minimum clearance required from mechanical constraints.

At the design windings separation, the dielectric strength of the gap may be exceeded at the peak terminal voltage at full speed. Therefore, a corona suppression system is incorporated into the insulation scheme to reduce the voltage placed across the gap.

Since the conductors are fully exposed to the applied magnetic field, the mechanical forces on the conductors are larger than the forces on conductors in conventional machinery, where the primary forces are exerted on the steel teeth which form the conductor slots. Since the moment of inertia of the conductors is only a small fraction of the total inertia, the ground plane insulation is loaded heavily in shear.

The conductors must be finely stranded and transposed to hold eddy current losses and the resulting temperature rise to an acceptable level. The strand size is smaller than that of conventional machinery because of higher base frequency (180 Hz vs 60 Hz). Also, the conductors are exposed to a peak magnetic flux density approaching 2.0 Tesla.

The rotor conductors are held in place against centrifugal forces by banding the rotor with a unidirectional fiberglass banding tape under 50,000 psi tension. This banding insures that the adhesive bond between the conductors and the ground plane insulation is not loaded in tension.

A partial cross-sectional end view of the air gap winding assembly is shown in Figure 4-25. One half of one rotor conductor and stator conductor are shown

Multi-layer windings and windings with crossovers, such as lap and spiral windings are avoided. A single-layer, multi-turn wave winding is used. The wave winding is modified to eliminate the crossover by removing one conductor under one pole and using the closely coupled compensating winding as the current return. See Figure 4-26. Both windings have one missing conductor and slip rings are required at both ends of the rotor.

Figure 4-25. Air Gap Winding Assembly

Figure 4-26. Modified Wave Winding

A developed view of the rotor winding design, taken at the outer radius of the ground plane insulation, is shown in Figure 4-27. The output lead is taken at the top of the vertical shaft machine and the jumper to the commutating winding is made at the bottom. Notice that there are twelve conductors per pole for three conductor belts, but only 11 conductors per pole for the fourth belt. The center line of two of the belts are shifted from $\pm 90^\circ$ to $\pm 87.6^\circ$ to counteract the missing conductor force caused by the difference in area between the 12 conductor belt at 180° and the 11 conductor belt at 0° .

Conductor Design

Each armature conductor consists of ten Type 8 Litz wires manufactured by New England Electric Wire Corporation, Lisbon, New Hampshire. Each Type 8 Litz wire is a flattened spiral tube rolled to a rectangular

Figure 4-27. Developed Armature Winding at Radius = 19.39 cm

cross section 16.5 mm by 5.08 mm (0.065 in. x 0.200 in.). Each Litz wire consists of 12 bundles of seven No. 30 AWG film insulated conductors stranded six around one. The wire construction is designated 12 x 7 x 30 AWG. Approximately 51 percent of the Litz wire cross-sectional area is filled with copper. Of the remaining area, 16 percent is filled by the film insulation and bunding cement, and 33 percent is void. The void areas must be filled with epoxy resin to maintain long insulation life.

The armature conductor is formed by winding the Litz wires ten in hand. The Litz wires are linearly wrapped with Hexcel F159/120 pre-impregnated fiberglass tape to serve as turn to turn insulation and provide mechanical strength. A mock up of one conductor, formed by ten parallel strands of insulated Type 8 Litz wire is shown in Figure 4-28. A photograph of the rotor during the winding operation is shown in Figure 4-29.

Figure 4-28. Mock-up of Armature Winding Conductor

Figure 4-29. Rotor During Ten in Hand Winding Operation

The primary filament wire size was selected to limit the temperature rise due to open circuit eddy current losses to an acceptable level over a period of 60 seconds. The one minute period used for this calculation is based on the estimated time that a system of full scale machines would be at maximum voltage during synchronization before a discharge pulse. The calculated adiabatic temperature rise is listed for several primary filament wire sizes in Table 4-7.

TABLE 4-7
ONE MINUTE TEMPERATURE RISE OF CONDUCTOR
DUE TO OPEN CIRCUIT EDDY CURRENT LOSS

<u>Wire Size (AWG)</u>	<u>Wire Diameter (mm)</u>	<u>$(T-T_0)(^{\circ}\text{C})$</u>
30	0.25	15.2
24	0.51	56.8
22	0.64	85.6
19	0.91	155

The temperature rise was calculated using Equation 4-5.

$$\Delta T = T - T_0 = \left[(1 + 2\alpha P_{ec} t)^{1/2} - 1 \right] / \alpha \quad (4-5)$$

Where α is the temperature coefficient of resistivity ($^{\circ}\text{C}^{-1}$), t is the elapsed time (60 seconds), and the open circuit eddy current power loss for an individual wire is given by Equation 4-6.⁴

$$P_{ec} = \sum_{n=1}^{\infty} \frac{\pi \sigma \omega_n^2 B_n^2 d_w^4 \ell}{128} \quad (4-6)$$

Where σ is the electrical conductivity (mho/m) evaluated at $T = T_0$, ω_n is the electrical angular frequency and B_n is the peak flux density of the n th space harmonic, d_w is the filament diameter (m), and ℓ is the effective wire length (m). It is assumed that the wire diameter is much smaller than a skin depth.

The cross-sectional area of the copper is sized to match the winding resistance of the machine to the impedance of the flash lamp load at peak current. The wires are made as radially thin as possible to minimize reactance. The rectangular shape chosen is also suitable for making the tight bend at the inner end turn radius.

The primary 30 AWG filaments are insulated with a thin (0.018 mm) film at thermosetting polyester with an amide imide overcoat rated at 200° C. A bondable high temperature cement (0.01 mm) is applied to the outside of the insulated conductor. The cement has a softening point of 130° C. The tradename of the system used is A.P. Bondeze, a registered trade mark of the Phelps-Dodge Corporation. This system is typically used in the construction of saddle yoke for television picture tubes.⁵

During the epoxy resin curing process, the conductors are heated to the 130°C softening point of the film adhesive. However, a temperature of 175°C is recommended to obtain a successful bond. Unfortunately, the Hexcel epoxy system cannot tolerate the higher temperature. Another insulation system, S-4 Bondeze, also manufactured by Phelps Dodge, has a lower cement softening point at 105°C. However, the thermoplastic polyurethane insulation is only rated at 130°C, and shorts could develop at localized hot spots during the cure of the epoxy resin.

It has been suggested that a strip of Hexcel pre-impregnated glass tape be inserted in the center of the spiral flattened tube of the Type 8 Litz wire. New England Electric Wire (NEEW) has had problems with this technique in the past (the pre-preg tape grabs and fails in tension), and at the present time is unwilling to manufacture the wire in this fashion. NEEW has successfully braided the wire around a polyimide film tape (e.g. DuPont Kapton).⁶

Turn to Turn Insulation

In the modified four pole multi-turn wave winding the maximum voltage between adjacent conductors V_{T-T} is four times the voltage per conductor. For the 6 kV engineering prototype with 47 conductors, the peak voltage between adjacent conductors is given by Equation 4-7.

$$V_{T-T} = 4 \times V_{OC}/47 = 4 \times 6kV/47 = 4 \times 128 \text{ volts} = 510 \text{ volts} \quad (4-7)$$

Although the magnet wire film insulation could withstand this voltage, it is probable that the film insulation could be damaged during assembly. It is also necessary to fill voids in the Litz conductor and to improve the mechanical strength of the assembly. Therefore, each Type 8 Litz wire was wrapped with Hexcel F159/120 glass filled epoxy tape prior to assembly.

The pre-impregnated glass tapes are fabricated by slitting a large roll to the desired width. Apparently, one half inch is the minimum practical width for slitting B stage tape systems.⁷ Since the individual Litz wires are small, a quarter inch tape is needed to make a smooth butt lap wrap. Therefore, a customary half lap wrap or a butt lap wrap was not possible, and an alternative method was required.

A multi-station machine was designed and fabricated to continuously wrap the Litz wire with the Hexcel turn to turn insulation in a linear manner. A photograph of the machine is shown in Figure 4-30.

The wire and the insulating tape are fed continuously, and are guided by Teflon rollers. The wrap is performed in four steps as shown in Figure 4-31.

Figure 4-30. Multi-Station Linear Insulation Wrapping Machine

Figure 4-31. Linear Insulation Wrap Process

The Hexcel F159/120 tape has a nominal thickness of 0.14 mm (5.5 mils). There are three layers of tape between adjacent wires for a total build of 0.42 mm (16.5 mils). Therefore, the nominal voltage stress (neglecting magnet wire insulation) is 12 kv/cm (30.4 VPM). It should be noted that all ten Litz wires are insulated in this fashion, even though only the outer wires of the ten wire conductor are adjacent to other turns. Again, this is done for mechanical integrity. A test sample of the insulated Litz wire is shown in Figure 4-32.

Figure 4-32. Sample of Insulated Litz Wire

Winding Transposition and Open Circuit Voltage Distribution

The individual Litz wires which form the conductor are wound ten in hand and connected in parallel at copper terminal plates at each end of the rotor. The multi-turn modified wave winding or serpentine winding forms a natural transposition. A litz wire that occupies the inside position of the conductor under the North poles, occupies the outside position under the South poles. The transposition is not perfect, since one conductor is missing and because the windings are shifted to counteract the missing conductor force.

The open circuit voltage distribution and the resulting circulating currents can be calculated if the harmonic content of the magnetic flux density is known and the positions of the windings are specified. The open

circuit voltage distribution was calculated for the prototype machine assuming an average peak flux density of 1.75 Tesla over an active length of 0.8 meters. The calculated voltages are listed in Table 4-8 for the second and sixth space harmonic for each of the ten Litz wires. The peak second harmonic circulating current is also given for each wire.

TABLE 4-8
PEAK OPEN CIRCUIT VOLTAGE AND CIRCULATING CURRENT DISTRIBUTION

<u>Wire Number</u>	<u>V_2 (kV)</u>	<u>V_6 (V)</u>	<u>I_2 (A)</u>
1	6.123	176.8	-7.34
2	6.134	179.7	-2.48
3	6.142	182.0	1.17
4	6.147	183.5	3.60
5	6.150	184.2	4.84
6	6.150	184.2	4.85
7	6.148	183.5	3.65
8	6.142	182.0	1.25
9	6.134	179.9	-2.36
10	6.123	176.9	-7.19

The total circulating current average power loss is approximately 26 watts. This calculation shows that a Roebel type transposition is not required to further reduce the open circuit eddy current loss for this winding scheme. A similar calculation should be performed to calculate circulating currents during the discharge pulse, to determine the current distribution under load.

Ground Plane Insulation System

The ground plane insulation must withstand the armature voltage to ground, nominally 6 kV peak, but most importantly, it must transmit the torque required to decelerate the inertia of the rotor laminations and shaft. The ground plane insulation design is based on the forces calculated during the

original circuit simulation, using an inductance variation four times greater than was actually measured. Therefore, the insulation was designed to withstand higher forces than were generated during the test program.

Based on the original performance calculations, the maximum average shear stress on the adhesive bond is 7.1 MPa (1030 psi) under normal operating conditions and 17.3 MPa (2500 psi) under fault. The stress concentration factor due to non-uniform flux distribution is estimated to be 1.5. Therefore, the insulation system is subject to a cyclic load of 10.6 MPa (1540 psi) under normal operation, and must withstand a peak stress of 26 MPa (3780 psi) under short circuit. The insulation is loaded in compression at the instant of peak sheat stress by the magnetic pressure in the air gap. The maximum compressive loading occurs 100 μ sec after the peak shear stress, and is a maximum of 33 MPa (4000 psi) under fault.

Hexcel F159/1581 (equivalent to F159/7781) was selected for the ground plane insulation system after a series of static rotary shear strength tests were performed on several candidate pre-impregnated "B"staged epoxy resin systems. The rotary shear test fixture is shown in Figure 4-33. The top half of the housing is removed to show the two inner mandrels. The test fixture is made of mild steel.

Figure 4-33. Rotary Shear Test Fixture

The fixture was cleaned prior to each test as follows:

1. Sand surfaces with 180 grit emery paper.
2. Degrease with soap and water.
3. Dip in solvent (methanol).
4. Soak jig 10-12 minutes in American Cyanamide Prebond 700 @ 85°C.
5. Rinse in distilled water.
6. Rinse in methanol.
7. Oven dry at 150°C.

The tape under test was supplied or cut to 2.5 cm widths and was wrapped on the mandrels with a total build greater than the housing bore. The tape was compressed approximately 15 percent when the mandrel housing was clamped tight. The calculated shear stresses for a variety of materials tested in this manner are listed in Table 4-9.

TABLE 4-9
TORSIONAL SHEAR TEST RESULTS

<u>Material</u>	<u>Shear Stress</u>	
	<u>MPa(psi)</u>	<u>Failure Mode</u>
G. E. Mica Mat 77937	2.06(300)	Adhesive
G. E. Mica Mat 77918	4.82(700)	Interlaminar
Dow DER 332 Wet Layup	13.8(2000)	Adhesive
Scotchply 1003	27.6(4000)	Interlaminar
Hexcel F159/7781	29.6(4300)	Interlaminar
Hexcel F159/7781	27.1(3939)*	Interlaminar
Hexcel F159/7781	18.7(2720)*	Machined Interface

*Tests performed at end of material shelf life (2 months at 40°F)

The stator ground plane insulation was fabricated using the Hexcel F159 resin system. Later, a similar resin system F155 was used for the rotor ground plane insulation. The typical interlaminar shear strength of the F155/1581 system is approximately 14 percent higher than the F159/1581 system based on Hexcel product bulletins.⁸ It is recommended that the F155 system be

used in future construction.

The tape is supplied in 5 cm width by 0.24 mm thick and is applied in a minimum of seven half lap wraps for a total build of at least 0.338 cm. The nominal dielectric stress, therefore, is 18 kV/cm (45 VPM). The peak dielectric stress anticipated is 30 kV/cm (76 VPM). The higher voltage can be impressed on the insulation in the event that the lamps fail to trigger. The maximum terminal voltage is clamped to 10 kV by the insertion of a dummy load across the PILC capacitor if the lamps do not trigger.

Centrifugal Force on Armature Winding

The centrifugal forces which act on the armature windings would load the adhesive bond in tension if no additional steps were taken to take the forces elsewhere. Since the adhesive bond strength in tension is relatively weak, and because the effect of tensile loads is to reduce the effective shear strength of the bond, our design approach was to maintain compressive loading of the bond at all times. This was accomplished by banding the rotor with a moisture resistant unidirectional fiberglass tape, impregnated with a "B" staged, fully catalyzed thermosetting polyester resin. The product used was a 60 end tape, Class F, manufactured by General Electric (No. 76843).

The centrifugal forces on the conductors generate a mechanical pressure at the inner radius of the banding wrap.

$$P_{\text{wrap}} = 1/2 \rho \omega_m^2 (r_o^2 - r_i^2) = 1.58 \text{ MPa} = 230 \text{ psi} \quad (4-8)$$

where ρ is the effective density of the Litz wire and insulation system (4708 kg/m^3), ω_m is the angular frequency of the rotor (sec^{-1}), and r_o and r_i are the outer and inner radii of the windings.

If the banding wrap is considered as a thin walled pressure vessel, the tangential stress in the banding wrap, σ_{wrap} is given by⁹

$$\sigma_{\text{wrap}} = \frac{P_{\text{wrap}} r_o}{t_{\text{wrap}}} \quad (\text{N/m}^2) \quad (4-9)$$

where t_{wrap} is the wrap thickness $\ll r_o$.

The recommended banding tension for G.E. 76243 (60 end) is 2330N (525 lb).¹⁰ The approximate cross sectional area of the tape is $5.806 \times 10^{-6} \text{ m}^2$. Therefore, the maximum tensile stress is given by

$$\sigma_{\text{wrap}} = \frac{2330}{5.806 \times 10^{-6}} = 401 \text{ MPa (58.3 ksi)} .$$

The minimum banding thickness is then

$$t_{\text{wrap}} = \frac{1.58 \times 10^6 \times 0.199}{401 \times 10^6} = 7.84 \times 10^{-4} \text{ m} = 30 \text{ mils} .$$

Since approximately 20 percent of the banding tension is lost during cure, the actual banding thickness must be proportionately higher. Therefore, a total of one millimeter (40 mils) of the banding was applied to the outer periphery of the rotor under 2330N tension. The tension was maintained using the KTD-100 Tension Device manufactured by Klein Armature Works, Inc., Centralia, Illinois.

Hot banding is recommended to assure that the maximum compressive force is retained on the armature coils, after the banding tape is cured and the armature is in use. General Electric tests have shown that hot banded tapes retain twice the applied tension (30%) that cold banded tapes retain (40%).¹¹ Therefore, the banding tape was heated using a hot air gun as the banding was wrapped around the rotor. See Figure 4-34.

Figure 4-34. Applying Glass Banding Tape to Rotor

It is important that the banding thickness be minimized to maintain as small a radial separation between the rotor winding and compensating winding as possible.

Winding Terminations

The ten parallel strands of Type 8 Litz wire are silvered soldered to copper plates at each end of the rotor. A low temperature silver bearing solder (All-State No. 430) is used to reduce the peak temperature experienced by the Litz conductor and turn to turn insulation. The ten parallel wires are tightly packed over the active length of the rotor. However, the center to center spacing of the wires is increased at the ends to reduce the unbalanced radial force acting on the rotor due to the magnetic pressure generated at the output terminals. The end termination fanout at one end of the rotor is shown in Figure 4-35.

Figure 4-35. End View of Rotor Showing Fanout of Litz Conductors

The 3.2 mm (0.125 inch) copper plate located at each end of the rotor is bolted and silver soldered to a 6.4 mm (0.25 inch) thick copper ring

23 cm in diameter (8.5 inch I.D.), which is in turn brazed to a 25.4 cm (10 inch O.D.) 2.5 cm (1 inch) wide copper slip ring. The copper slip ring fits tightly onto a cast epoxy pad machined to the inner slip ring diameter. The slip ring is locked into position by a thin steel nut that screws onto a threaded section of the bearing journal (shaft nut). The steel nut is insulated from the slip ring by G-10 washers.

An additional copper ring, located at the exterior of the rotor end termination (visible in Figure 4-35) serves as a slip ring for two miniature brushes which are included in the corona suppression system. This ring is insulated from the main slip ring and end terminating plate by a 3.2 mm (0.125 inch) thick G-10 sheet.

Brushgear

The armature current is collected at each end of the rotor by a set of thirty copper graphite brushes. The brush material, Morganite CMIS, is silver soldered to a 1.6 mm (1/16 inch) copper strap which serves as a cantilever beam (trailing arm) to locate the brushes mechanically as well as conduct armature current. The copper straps are bolted to tabs mounted on a copper ring which serves as the armature winding output terminal. A photograph of one brush assembly is shown in Figure 4-36 (the top G-10 cover is removed).

Figure 4-36. Top View of Armature Brush Assembly

Each brush has an apparent contact area of 3.95 cm^2 ($2.22 \text{ cm} \times 1.78 \text{ cm}$). The maximum slip ring velocity is 70 m/sec. The brushes are actuated by air cylinders, clevis mounted in G-10 rings. The brushes are normally spring lifted off of the slip ring surface to reduce friction loss and wear. The brushes are pressed against the slip ring by pressurizing the air cylinders one second after energizing the alternator field power supply. The brushes are then lifted by the operator after the discharge sequence has been completed and the field flux has decayed to remanent flux conditions.

The output conductor rings are fabricated of ETP copper. The rings are grooved to help provide equal current sharing among the brushes.

Corona Suppression System

The radial separation between the rotor winding and the compensating winding must be made as small as possible to achieve the high performance required of the machine. The air in the gap would be stressed near its breakdown strength if required to hold off the maximum armature voltage. Therefore, the prototype machine was designed with a corona suppression system to shunt the air gap with a (relatively) low impedance so that the voltage stress is carried by the solid dielectric.

This design concept was proposed at the July, 1978 design review (held at Lawrence Livermore Laboratory) to maintain the high performance specification of the 2.5 megajoule 12 kilovolt machine which was then under consideration. That machine had the same physical air gap as the prototype compensated pulsed alternator, but twice the rated voltage.

The action of the system can be seen using the simplified circuit model shown in Figure 4-37.

The circuit parameters used in Figure 4-37 are given below:

$$R_R = (\ell/2\pi r_{or})(\rho/t) = 0.97(1000\Omega/\text{sq}) = 970\Omega$$

$$R_S = (\ell/2\pi r_{is})(\rho/t) = 0.96(1000\Omega/\text{sq}) = 960\Omega$$

$$C_R = 3.17 \times 10^{-8} \text{ F}$$

$$C_S = 5.67 \times 10^{-8} \text{ F}$$

$$C_{air} = 8.22 \times 10^{-9} \text{ F}$$

where R_R is the resistance of the rotor semiconductive surface, R_S is the

Figure 4-37. Simplified Circuit Diagram - Corona Suppression System

resistance of the stator semiconductive surface, C_R , C_S and C_{air} are capacitances of the rotor side air gap insulation, stator side air gap insulation, and air gap itself. The terms l , r_{or} , and r_{is} are rotor length, outside rotor radius, and stator bore radius; (ρ/t) is the ohms per square of the semiconducting layers. The two semiconductive surfaces are connected by small graphite brushes.

The effective time constant of the air gap capacitance and shunt resistance is approximately 4.0 microseconds. Under steady state operation at 180 Hz, the calculated peak voltage at the center of the air gap is 1.1 percent of the source voltage. At 2315 Hz, $(2\pi\sqrt{27\mu H \cdot 175\mu F})^{-1}$ the voltage is reduced to 14 percent of the source voltage.

It is realized that at very high frequency or for an impulse, the voltage divides capacitively and the air gap voltage is approximately 71 percent of the total voltage difference. Also, this simplified model treats the compensating winding and armature winding as solid cylinders. The actual voltage distribution varies with both axial and angular position. A detailed two dimensional RC network model has been used to calculate the voltage distribution for the serpentine winding configuration.

Since a terminal fault occurred at the peak design voltage of the machine, it appears that it may be necessary to increase the physical

air gap and reduce field intensification at the ends of the machine. If this were done, the air gap could be designed to hold off peak machine voltage with some additional margin. It will then be necessary to modify the winding configuration to prevent increasing the minimum inductance significantly. If a larger physical air gap is used, it is recommended that the semiconductive layer be retained to reduce the maximum electric field stress placed on the gap, to provide an additional factor of safety.

Oven Cure of Rotor Armature Winding

The rotor ground plane insulation and the turn to turn insulation were cured by heating the rotor in a forced convection oven which was fabricated in house specifically for this purpose. A schematic diagram of the oven is shown in Figure 4-38.

The rotor was suspended vertically from the overhead beam by a chain hoist. A fabricated sheet metal enclosure was assembled around the rotor and insulated with fiberglass batting. The air was circulated by a squirrel cage blower driven by a 220 volt, 3 phase, 685 rpm induction motor. Twelve nichrome element power resistors (U-shaped with stainless steel housing and cooling fins, rated at 240V 7KW) were mounted directly beneath the rotor, and connected electrically in parallel. The heating resistors were driven by a full wave half-SCR controlled DC power supply. The current in the resistors was controlled to maintain an average air temperature of 149°C (300°F). The air flow rate at the rotor periphery was approximately 6.1 m/sec (20 fps).

The flow rate and air temperature were selected to heat the rotor as quickly as possible, but keeping the maximum temperature of the shrink film and banding tape within 135°C (275°F). A thermal equivalent circuit was used to model the rotor insulation system to predict the time required to heat the bonding surface to the average temperature of 120-126°C. Each element was modeled by a RC T network using thermal resistance and heat capacity of the material. The equivalent circuit for curing the rotor ground plane insulation is shown in Figure 4-39.

The calculated time to heat Node 4, the interface between the rotor laminations and the Hexcel ground plane insulation was 5.9 hours. The rotor was heated to this temperature in approximately 6.3 hours.

POWER SUPPLY AND OVEN SCHEMATIC

Figure 4-33. Schematic Diagram of Oven for Rotor Armature Insulation Cure

BIRD
UT-CEM
2-27-80

Figure 4-39. Equivalent Thermal Circuit for Curing Rotor Ground Plane Insulation

The time was increased slightly because a layer of shrink film tape was wrapped around the outside layer of the Hexcel ground plane insulation, and the actual thickness of the Hexcel insulation was greater than originally modeled.

A similar circuit model was used to calculate the time required to heat the machined ground plane insulation, turn-to-turn insulation interface to the recommended curing temperature. The circuit model used for this calculation is shown in Figure 4-40. The calculated heating time was 8.0 hours.

The thermal resistance values and heat capacities used for this calculation are listed in Table 4-10.

The recommended cure cycle is to raise the material (Hexcel F155/1581, F159/120) to 121°C (250°F) at a rate of 1.1 - 5.6°C/min (2-10°F/min), and then hold the material at temperature for 90 minutes. With the large thermal capacitance of our system (primarily rotor laminations), we required a much longer time to reach temperature. Fortunately, the Hexcel resin system used is very forgiving and the local Hexcel representative stated that some large pieces have required as many as 13 hours to reach temperature. [12] The important thing to consider is that pressure must be

Figure 4-40. Equivalent Thermal Circuit for Curing Turn-to-Turn Insulation

TABLE 4-10
THERMAL RESISTANCE AND HEAT CAPACITIES OF ROTOR MATERIALS

<u>Thermal Resistance</u>	<u>(°C/watt)</u>	<u>(°F-hr/Btu)</u>
RSHAFT	6.84×10^{-4}	3.61×10^{-4}
RILAMIN, ROLAMIN	4.91×10^{-3}	2.59×10^{-3}
RIEPOXY, ROEPOXY	4.06×10^{-3}	2.14×10^{-3}
R1LITZ, R6LITZ	1.63×10^{-4}	8.60×10^{-5}
R2LITZ, R3LITZ, R4LITZ, R5LITZ	3.26×10^{-4}	1.72×10^{-4}
RI HEXCEL, RO HEXCEL	4.82×10^{-4}	2.54×10^{-4}
RIBAND, ROBAND	1.13×10^{-3}	5.95×10^{-4}
RISHRINK, ROSHRINK	2.12×10^{-4}	1.12×10^{-4}
RCONVEC	2.16×10^{-2}	1.14×10^{-2}
<u>Heat Capacities</u>	<u>(Joule/°C)</u>	<u>(Btu/°F)</u>
CSHAFT	3.65×10^4	1.92×10^1
CLAMIN	4.67×10^5	2.46×10^2
CEPOXY	1.56×10^4	8.22×10^0
C1LITZ, C3LITZ	3.95×10^3	2.08×10^0
C2LITZ	7.90×10^3	4.16×10^0
CHEXCEL	9.72×10^2	5.12×10^{-1}
CBANDING	4.69×10^3	2.47×10^0
CSHRINK	3.42×10^2	1.80×10^{-1}
<u>Boundary Condition</u>	<u>(°C)</u>	<u>(°F)</u>
VAIR	1.49×10^2	3.00×10^2

applied continuously during the temperature rise, since the flow characteristics of the resin deteriorated with time at elevated temperatures. The recommended cure pressure is 0.14-0.21 MPa (20-30 psi). The shrink film was used to supply pressure when curing the ground plane insulation (12% shrinkage), while the banding wrap provided pressure when curing the turn to turn insulation. It should be noted that the thermal time constant of the ground plane insulation is over two minutes (127 seconds) and the thermal time constant of the banding wrap is over 10 seconds. Therefore, the windings are thermally insulated during the length of the pulse, and the assumption of adiabatic heating is valid for that time period.

Armature Fabrication and Curing Operation-Summary

The following procedure was used in fabricating the air gap armature winding.

1. Grind rotor O.D. to provide smooth steel surface
2. Etch rotor surface (25% nitric acid solution) to remove burrs formed during grinding operation
3. Rinse rotor surface with water
4. Treat rotor with American Cyanimide Prebond 700 @85°C (Basic)
5. Heat rotor to bake off remaining residue and dry thoroughly
6. Wrap rotor surface with F155/1581 Hexcel impregnated glass tape. Build to thickness greater than 0.38 cm (150 mils). Wrap with shrink tape.
7. Cure rotor in oven (150°C air temperature)
8. Cast epoxy slip ring insulation
9. Turn cured ground plane insulation and slip ring insulation to final diameter on lathe.
10. Bond G-10 winding spacers to rotor surface with room temperature cure epoxy
11. Strip magnet wire film insulation with fused salt bath at one end of 10 Litz wires. Rinse and tin with silver solder.
12. Wind Litz wires ten in hand with rotor in lathe or winding stand. Litz wires have been previously wrapped with turn to turn insulation.
13. Insert "S" shaped strip of Hexcel F155/1581 between adjacent conductors (1 conductor = 10 Litz wires) to improve turn to turn insulation. (See Figure 4-41)

ARMATURE WINDING CONSTRUCTION

12 CONDUCTORS PER POLE
10 TYPE 8 LITZ WIRES PER CONDUCTOR

Figure 4-41. Rotor Winding Manufacturing Detail
View Over One Pole Pitch

14. Pack conductors in place using triangular shaped wedges at G-10 spacer.
15. Strip and tin Litz wires at final end termination
16. Silver solder Litz wires into copper end plates
17. Assemble main slip ring
18. Wrap rotor with shrinkable film tape (G.E. No. 76851)
19. Roll rotor on granite surface table to pack wires
20. Wrap rotor with fiberglass banding tape (G.E. No. 76843) under 2330N (525 lb) tension. Peel shrink tape approximately four inches ahead of banding to prevent conductors from bulging.
21. Rewrap banded rotor with shrinkable film tape
22. Heat rotor in oven to 130°C
23. Remove shrinkable film tape
24. Seal with spray coat of insulating paint (G.E. No. 1201)
25. Bake insulating paint with heat lamps
26. Coat outer surface with semi-conducting paint (G.E. No. 9919 Black) Build up thickness to achieve 1000 Ω /square. Bake under heat lamps.

A photograph of the completed rotor, taken during the baking of the insulating paint is shown in Figure 4-42.

Figure 4-42. Baking Rotor Insulating Paint Seal Coat

Compensating Winding Design and Fabrication

The compensating winding is not subjected to the 180 Hz alternating magnetic field imposed on the armature winding. Therefore, the conductor need not be as finely stranded as the armature conductor. However, the winding is otherwise identical to the armature winding. It was convenient to fabricate the compensating winding of the same Litz wire conductor to avoid additional tooling fabrication. Also, the flexible conductor was amenable to the serpentine winding configuration.

The compensating winding was fabricated similarly to the rotor with two major differences. First, the ground plane was wound on the outer diameter, so that it was not necessary to cure the ground plane insulation separately. Second, the radial pressure was applied by clamping the poles tightly around the ground plane insulation, rather than relying on the banding or shrinkable film.

The compensating winding was wound on a collapsible mandrel fabricated from aluminum plate and steel angle. The mandrel was turned on a lathe to a radius equal to the inner radius of the stator air gap insulation. See Figure 4-43. Next, 8 layers of polyimide insulating tape (Rogers Corporation R-7021, 2 mil DuPont Kapton, 1 mil Azelate adhesive) was wrapped under tension (adhesive side out) to form a radial build of approximately 0.61 mm (24 mils). The insulated mandrel was then wrapped with shrink film and cured at 300°F. The mandrel was heated using nichrome element resistors (same as those used in rotor oven). The resistors were mounted on aluminum plates and covered with Thermon heat transfer cement. These resistor plates were then clamped against the inside walls of the aluminum mandrel.

G-10 winding spacers were then bonded to the air gap insulation using Hexcel resin and were then cured in position. The insulated Litz wires were then wound ten in hand as was done on the rotor. Next, seven half lap wraps of Hexcel F159/1581 were wrapped around the winding.

The steel poles, stainless steel pole supports, and stainless steel end turn support rings were clamped around the mandrel. The inside surface of these pieces were painted with Hexcel F159 resin prior to assembly. To avoid the formation of wrinkles in the tape, the pole pieces were rotated into place rather sliding at the interface.

Figure 4-43. Compensating Winding Assembly

Heating plates, fabricated from the nichrome element resistors and Thermon heat transfer cement, were then inserted inside the mandrel, and were strapped to the outside of the pole assembly. The entire system was then covered with fiberglass batting insulation. The assembly was heated for four hours to reach cure temperature. After ten hours the mandrel temperature had fallen from 136°C to 111°C (277°F to 232°F). The insulation was then removed and the assembly allowed to cool.

The ten Litz wires, which had been previously stripped of insulation and tinned, were soldered to copper plates which serve as the grounded output terminal (top) and jumper terminal (bottom). Although our schedule did not allow this, it is preferable that the compensating winding be fabricated after the rotor winding since the inner diameter of the stator is easier to hold to design tolerance than is the outer diameter of the rotor.

The stator winding assembly was successful with the exception of the polyimide air gap insulation. At the time that the mandrel was removed from the stator bore after the cure cycle, the air gap insulation

appeared to have bonded satisfactorily. However, the tape then relaxed and wrinkled to the point that it could not be used.

The published interlaminar peel strength of the adhesive is 1398 N/m (8.0 lb/in.).¹³ The calculated radial force per unit length required to hold the tape against the 111 N (25 lb) winding tension is only one half this value. Unfortunately, the applied tension could not be controlled at these low levels consistently, and the applied tension may have exceeded 266 N (60 lb) in some places. In addition, the winding mandrel was split in four pieces for disassembly. The adhesive strength of the tape was reduced along these grooves because of the absence of applied pressure during the cure cycle. Interlaminar adhesive failure was pronounced in these areas.

The air gap insulation was later peeled out of the stator bore. The bore was then sealed with multiple layers of insulating paint, the same system as was used on the rotor. Finally, the semiconducting paint was sprayed over the insulating paint to serve as a conducting layer for the corona suppression system.

Compensating Winding Fabrication and Curing Operation - Summary

The following procedure was used in fabricating the air gap compensating winding:

1. Fabricate collapsible winding mandrel. Machine outer diameter of mandrel to inner diameter of air gap insulation.
2. Wrap mandrel with air gap insulation system. Wrap slick side down.
3. Cure air gap insulation system (electric heaters in bore of mandrel).
4. Bond G-10 winding spacers to wrapped mandrel, cure epoxy if not room temperature system.
5. Strip magnet wire insulation with fused salt bath. Tin ends of 10 Litz wires.
6. Wind Litz wires ten in hand with mandrel in winding stand. Litz wires have been previously wrapped with turn to turn insulation.
7. Insert "S" shaped strip of Hexcel F155/1581 between adjacent conductors.
8. Pack conductors in place using triangular shaped wedges at G-10 spacer.
9. Strip magnet wire insulation and tin Litz wires at final end.

10. Wrap winding with F159/1581 Hexcel pre-impregnated glass tape. Build to thickness of approximately 0.38cm (150 mils).
11. Clean poles, pole supports, and stainless steel end turn support rings with American Cyanamide Prebond 700 @ 85°C.
12. Paint bond surface with Hexcel resin.
13. Rotate pole pieces into position.
14. Using hydraulic jacks, clamp poles to compress Hexcel tape 12 percent.
15. Insert dowel pins and torque down bolts.
16. Clamp heating resistors inside the bore of mandrel and around periphery of pole assembly. Cover with insulating fiberglass blanket.
17. Heat bond surface to 120-125°C.
18. Solder copper output plates to tinned Litz wires.
19. Remove aluminum winding mandrel from bore.
20. Spray bore with insulating paint seal coat. Bake to dry.
21. Spray bore with semi-conducting paint for corona suppression. Build up thickness to achieve 1000Ω/square. Bake to dry.

Auxiliary Components

The auxiliary components that are used in the compensated pulsed alternator system include the following:

1. 125 Hp variable speed DC motor drive
2. 120 kW DC exciter
3. Low pressure oil supply system
4. High pressure oil supply system
5. Controls and Instrumentation.

Variable Speed DC Motor Drive

A 125 Hp DC motor drive was selected for the prototype system to provide variable speed control at a reasonable cost. The initial cost of the DC drive system is considerably less than an AC drive of the same rating because of the higher cost of the adjustable frequency speed controls for the AC system. The primary advantage of the AC adjustable speed drive is the elimination of maintenance associated with the DC motor commutator and brushgear.

The 5400 rpm rotor speed of the compensated pulsed alternator is more than a factor of two higher than the speed of prime movers with the required power rating. Therefore, a speed increase is required. A 2:51:1 increase in speed was obtained using a Uniroyal Power Grip High Torque Drive (HTD) timing belt and pulley system. The higher performance rating of this system is due to a new tooth design that improves stress distribution and allows higher overall loading than conventional timing belts or V belts.¹⁴ Since the HTD belt teeth mesh with sprocket grooves, there is no slippage to cause speed variations. Since friction is not required to pull the load, belt tension and heat build up are reduced.

The HTD belt is run around two aluminum pulleys with steel bushings, manufactured for this application by H.M. Manufacturing Company, Chicago, Illinois. "Off the shelf" cast iron pulleys are not rated for these speeds at the required pulley diameters. The pulleys are mounted on steel jackshafts which are supported by pillow block bearings on a frame constructed of steel box beam. The motor pulley is connected to the motor shaft by a dodge type duplex roller chain coupling. The load pulley is connected to the alternator shaft by a shear pin coupling. The shear pins are designed to fail during the discharge pulse to limit the drive belt tension and to protect the drive motor from rapid deceleration of the load.

Idler pulleys are mounted against the tension side and the slack side of the belt to reduce belt vibration at high speed. However, there is significant tooth noise generated at the smaller load pulley. The major disadvantage of the HTD belt drive is the audible noise. It is recommended that the drive system be muffled to reduce the noise level in the laboratory.

The primary components of the DC motor drive system are listed in Table 4-11.

At the time that the high speed runs were completed, the motor drive system was performing as required. However, it was necessary to solve a commutator flashover problem that occurred when the motor was first run above 4000 rpm. There are multiple causes of commutator flashover and no single cause could be identified. However, the problem was overcome by taking the steps listed on the following page.

TABLE 4-11
PRIMARY COMPONENTS OF 125 Hp DC MOTOR DRIVE

<u>Component</u>	<u>Manufacturer</u>
125 Hp 500 V 1750 rpm DC Motor, Frame size 407AT (field weakened to 2150 rpm) Shunt-stabilized	Powerton, Inc.
125 Hp Motor controller 500/300 V DC	Robicon, Inc.
145 KVA Isolation transformer 460/460 V	Sylvania
35.651 cm Pitch diameter HTD sprocket P80-14M-115	H. M. Manufacturing
14.260 cm Pitch diameter HTD sprocket P32-14M-115	H. M. Manufacturing
3500-14M-115 HTD belt	Uniroyal
Double Roller Chain Coupling - Model 8020	Morse
Double Shear Pin Coupling	UT-CEM

Steps taken to overcome commutator flashover problem:

1. Change voltage tap on isolation transformer to reduce peak AC voltage (increase conducting angle on SCR's in drive controller).
2. Decrease field weakening resistance (raise voltage to 550 volts).
3. Decrease acceleration rate reference voltage in drive controller.
4. Increase alternator bearing oil inlet temperature from 32°C to 52°C (90°F to original 125°F design temperature) to reduce bearing losses by lowering viscosity.
5. Reposition brush holders to correct spacing at 90 degrees.
6. Shim brush holders to remove excessive clearance between brush and holder.
7. Reseat brushes using fine sandpaper.
8. Add forced ventilation - separate blower at commutator end of motor.
9. Replace louvered vents at commutator end with plexiglass covers.
10. Add stiffening bar in belt drive mounting system.

Field Exciter

The field coils are excited by a three phase full wave rectified, fan cooled DC power supply rated at 1200 amps, 100 volts continuous. The 120 kW exciter is energized at the beginning of the discharge sequence by

closing a magnetic contactor on the primary side of the step down transformer included in the supply. The coils are charged to a peak current of 1275 amps in approximately three seconds. The alternator is discharged at 3.5 seconds, and the primary contactor is opened four seconds after the sequence is initiated.

Low Pressure Bearing Oil Supply System

The low pressure bearing oil supply system provides lubricating oil to the hydrodynamic journal bearings and thrust bearing. The bearings require a flow rate of 0.31 l/sec (5 gpm) at 0.1-0.14 MPa (15 to 20 psi). The system is driven by a 2 hp, 1800 rpm 460V - 3 phase induction motor mounted on a 379 liter (100 gallon) reservoir. The motor is coupled to a Hydreco pump Model No. 23PL190554. The reservoir oil temperature is controlled by modulating chilled water flow to a oil-water heat exchanger mounted on the reservoir. An identical motor and pump is used as the main scavenge pump for the lubricating system. A separate fractional horsepower pump is used as a secondary scavenge pump for the lower seal assembly of the upper journal bearing. The pump motor starter control logic is designed to energize the scavenge pumps if either the high pressure pump or the low pressure pump is started. The scavenge pumps can also be started independently.

High Pressure (Hydrostatic Lift) Oil Supply

A separate high pressure pump, drive motor, and reservoir is supplied to provide 5.5 MPa (800 psi) oil pressure to the hydrostatic lift pocket at the bottom of the thrust bearing housing. This allows the compulsator to be started and operated at low speed (less than 400 rpm) without a metal to metal rub which would wear the Babbitt pads. The high pressure system reservoir is connected to the low pressure system reservoir so that common scavenge lines may be used.

Controls and Instrumentation

The prototype compensated pulsed alternator is controlled manually during acceleration from rest to full speed. After the operator presses the discharge button, the discharge sequence is performed automatically using high threshold solid state logic (LDC-40 Line-O-Logic controller

manufactured by Automatic Timing and Controls Corporation, King of Prussia, Pennsylvania) and a NOVA II minicomputer. The sequence of operation was described in Section 2. A photograph of the main operators console is shown in Figure 4-44.

Figure 4-44. Operators Control Console

Instrumentation is provided to monitor the following machine parameters during an experiment. Each machine parameter and recording device are listed below:

1. Rotor speed
 - (a) Digital tachometer - visual display
 - (b) Analog tachometer (DC motor drive) - panel meter
 - (c) Analog tachometer - light beam oscillograph, FM tape recorder
 - (d) NOVA minicomputer - A to D converter
2. Terminal voltage - oscillograph, storage scope, tape recorder
3. Output current - storage oscilloscope
4. Circuit breaker current - storage oscilloscope
5. Rotor Position - NOVA minicomputer A/D converter
6. Field coil current - panel meter

7. Field coil voltage - panel meter
8. Upper journal bearing temperature - panel meter
9. Lower journal bearing temperature - panel meter
10. Thrust bearing temperature - panel meter
11. Upper journal bearing supply pressure - panel meter
12. Lower journal bearing supply pressure - panel meter
13. Thrust bearing supply pressure - panel meter
14. Brush pneumatic control supply pressure - panel meter
15. Top N-S shaft displacement - oscillograph, tape recorder
16. Top E-W shaft displacement - oscillograph, tape recorder
17. Bottom N-S shaft displacement - oscillograph, tape recorder
18. Bottom E-W shaft displacement - oscillograph, tape recorder
19. Top bearing housing accelerometer - tape recorder
20. Bottom bearing housing accelerometer - tape recorder
21. Axial shaft displacement - tape recorder
22. Hydrostatic lift pressure - panel meter

Additional GO, NO-GO interlock or warning signals include:

1. Coolant flow
2. Field coil temperature
3. Bearing temperatures
4. Bearing pressures
5. Hydrostatic lift pressure

Rotor Position and Speed Calculation

The speed and angular position of the rotor are very important to the performance of the prototype compulsator as a pulsed power supply. The flashlamps and series ignitron must be triggered at the proper instant to obtain maximum energy delivery. The timing system must be designed to allow operation over a wide range of speed and firing angle to determine the optimum combination of parameters and the verify circuit simulation models. The timing system must be designed to allow for factors such as the 12 msec mechanical delay associated with opening the Ross vacuum circuit breaker. This delay results in the need to trip the breaker before the flashlamps and series ignitrons are triggered.

The UT CEM NOVA minicomputer is used to generate a five volt positive going trigger pulse that initiates the automatic discharge sequence by triggering the eight channel LLL timing chassis.

The speed and position of the rotor are determined using a five volt high to low level signal which is generated by a photo-diode and transistor assembly mounted above the upper bearing housing of the alternator. A mechanical interrupt is bolted to a dowel pin which aligns the alternator half of the shear pin coupling on the alternator shaft. Once per revolution, this interrupt blocks the path between the photo diode and photo transistor, turning off the transistor and driving the collector voltage to supply voltage. A second transistor is used to invert the signal to improve noise immunity. When the interrupt clears the path, the output signal goes from the low level state (1.5 volts) to the 5.0 volt high level state. The location of the one per revolution signal electronics is shown in Figure 4-45. The signal is fed into a A/D converter of the NOVA minicomputer.

Figure 4-45. One per Revolution Photo Diode-Transistor for Position and Speed Calculation

The computer counts the number of times that a loop of program commands is executed between the first low to high logic level transition and the following low to high logic level transition. The mechanical period T is equal to the counter multiplied by the time required to execute the loop (loop time). The time per mechanical degree Δt is then given by Equation 4-10.

$$\Delta t = \text{COUNT} \times \text{LOOP TIME} / 360^\circ \quad (4-10)$$

The delay time Y associated with the firing angle $\Delta\theta$ (degrees before minimum inductance - defined as 0 degrees) is given by Equation 4-11.

$$Y = (\Delta\theta + 275)\Delta t \quad (4-11)$$

It is necessary to add 275 degrees to the value of $\Delta\theta$ because the one per revolution signal is generated 275 mechanical degrees after the minimum inductance position. An additional delay time C is provided to allow the Ross circuit breaker to start opening.

The computer program then calculates the number of loop counts after the next one per revolution signal to generate the trigger signal. The trigger signal then occurs C seconds prior to the time that the rotor is $\Delta\theta$ degrees ahead of the minimum inductance position:

$$\text{COUNT} = \frac{NT - (C+Y)}{\text{LOOP TIME}} \quad (4-12)$$

N is the integral number of periods T necessary for $\text{COUNT} > 0$. A typical timing diagram is shown in Figure 4-46.

Using this computer program it is possible to calculate the mechanical period within .01 μsec and to trigger the flashlamps within one degree of the desired firing angle. It is assumed that the rotor speed does not change over the time NT .

BIRD
UT CEM
2-29-81

Figure 4-46. Timing Diagram - Firing Angle Control

Pulsed Alternator Loss Analysis

The primary compensated pulsed alternator losses have been described in a previous report.¹⁵ The losses are summarized below:

1. Laminated rotor core loss
 - (a) Eddy current loss
 - (b) Hysteresis
2. Stray eddy current loss
 - (a) Pole face
 - (b) End turns
3. Armature copper loss
 - (a) Open circuit eddy current loss
 - (b) Transport current i^2R loss
 - (c) Armature eddy current loss
4. Bearing and seal friction
5. Windage
6. Brush friction and contact drop
7. Field i^2R loss

The motoring system is sized to accelerate the alternator rotor to 5400 rpm in a period of 90 to 120 seconds against the open circuit rotational losses. To reduce the torque required to accelerate the rotor, the main field is not excited until the rotor has reached full speed. Also, the brushes are lifted off of the slip ring surface during the motoring phase of the experiment. The brushes are not actuated until the discharge sequence has been started. Engineering prototype spin up and coast down curves are shown in Figure 4-47.

During motoring the rotor angular velocity is calculated using Equation 4-13.

$$\omega_m = (T_{app} - \Sigma T_{loss})/J \quad (\text{sec}^{-2}) \quad (4-13)$$

where J is the polar moment of inertia (joule-sec^2), T_{app} is the mechanical torque applied at the shaft and ΣT_{loss} is the sum of bearing drag, windage, and seal friction. The following expressions are used for calculating the loss torque:

ENGINEERING PROTOTYPE SPIN UP AND COAST DOWN CURVES

BIRD
 UT-CEM
 LLL
 4-9-79

Figure 4-47. Typical Motor/Coast Curves

$$T_{JBrg} = 0.162 \omega_m^{0.9031} \quad \text{N-m} \quad (4-14)$$

$$T_{IBrg} = 0.262 \omega_m^{0.569} \quad \text{N-m} \quad (4-15)$$

$$T_{seal} = 1.08 \quad \text{N-m} \quad (4-16)$$

$$T_{windage} = 4.90 \times 10^{-5} \omega_m^{1.5} + 1.16 \times 10^{-15} \omega_m^2 \quad \text{N-m} \quad (4-17)$$

The journal bearing and thrust bearing loss torque are based on curve fits to manufacturers loss curves.¹⁶ The journal bearing torque is adjusted to account for the longer pads of the prototype bearings. The published loss data is based on using light turbine oil with a viscosity of 150 SSU at 38°C (100°F) at an oil outlet temperature of 66°C (150°F). The prototype bearings are designed to operate with an inlet temperature of 49°C (120°F) and a 66°C (150°F) outlet temperature. At 5400 rpm the calculated total bearing loss is 33.5 kW (45 hp). During the initial stages of the testing program the inlet oil temperature was often no more than 27°C (80°F). The viscosity of the oil at 27°C is a factor of 2.8 higher than the viscosity at the design inlet temperature.^[17] At low speeds (3600 rpm or less) the difference in viscosity is not critical. However, at higher speeds the increased bearing drag causes higher armature current in the dc motor, contributing to the commutator flashover problem. It is recommended that the temperature of the oil reservoir be raised to a minimum of 38°C (100°F) at the start of an experiment so that the inlet temperature at full speed is 49°C (120°F).

The full speed losses include the mechanical losses listed above plus the losses associated with the applied magnetic field and the brush loss. These additional loss torques are listed below:

$$T_{core} = 8.12 \times 10^{-2} (B/1.9)^2 \omega_m^2 \quad (\text{N-m}) \quad (4-18)$$

$$T_{shaft} = 3.13 \times 10^{-2} (B/1.9)^2 \omega_m^2 \quad (\text{N-m}) \quad (4-19)$$

$$T_{Litz} = 4.80 \times 10^{-3} (B/1.9)^2 \omega_m^2 \quad (\text{N-m}) \quad (4-20)$$

$$T_{Corona} = 1.40 \times 10^{-3} (B/1.9)^2 \omega_m^2 \quad (\text{N-m}) \quad (4-21)$$

$$T_{\text{circ}} = 8.13 \times 10^{-5} (B/1.9)^2 \omega_m^2 \quad (\text{N-m}) \quad (4-22)$$

$$T_{\text{brush}} = 8.46 \times 10^1 \quad (\text{N-m}) \quad (4-23)$$

B is the peak radial magnetic flux density, T_{core} is the rotor hysteresis and eddy current drag, T_{shaft} is the shaft eddy current drag, T_{Litz} is the armature open circuit eddy current loss torque, T_{Corona} is the eddy current loss torque generated in the semiconductive surface on the rotor periphery, T_{circ} is the armature circulating current loss torque, and T_{brush} is the total brush drag. Full speed rotational losses are listed in Table 4-12.

TABLE 4-12
CALCULATED FULL SPEED (5400 rpm) ROTATIONAL LOSSES

	<u>Loss (kW)</u>
Journal Bearings (Total)	28.03
Thrust Bearing	5.50
Seals	0.61
Windage	2.47
Rotor Core Loss	25.97
Shaft Eddy Current Drag	0.90
Semiconducting Surface Eddy Current Drag	0.45
Armature Open Circuit Circulating Current Loss	0.03
Brush Drag	47.84
TOTAL	121.31

The total full speed losses total 122 kW (163 hp). To maintain constant speed, the dc motor armature current must be held at 130 percent of rated current. The motor is capable of 150 percent rated current for one minute. The motor controller is rated at 150 percent rated current continuously. The field is energized for four seconds and the brushes are lifted within one minute. Therefore, the motor drive is not loaded beyond its short time rating.

The terminal fault occurred before exhaustive loss analysis could be performed. However, the motoring time and coast down times did not vary significantly from the curves shown in Figure 4-46 once the inlet oil temperature was raised to 49°C. The motor held speed against the field related losses and brush drag successfully. However, it is recommended that the dc motor armature current and voltage be recorded in future experimental runs to verify the calculated losses.

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5. SUMMARY OF THE PROTOTYPE COMPULSATOR TEST PROGRAM

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5. SUMMARY OF THE PROTOTYPE COMPULSATOR TEST PROGRAM

The goal of the engineering prototype test program was to demonstrate the feasibility of using a rotating electrical machine, designed specifically for pulse power duty, to drive xenon flashlamps in a suitable mode for pumping solid state laser amplifiers. The design objective was to deliver 200 kilojoules to sixteen 15 x 20 mm x 112 cm parallel flashlamps in approximately one millisecond.

The prototype compensated pulsed alternator was successfully discharged into the flashlamp load a total of twenty seven times from speeds ranging from 33 percent to 90 percent of the maximum design speed (5400 rpm). The maximum energy actually delivered to the flashlamp load was 135 kilojoules from a rotor speed of 4840 rpm. The peak load current was 30 kiloamps, with a half width of 1.3 milliseconds. The energy delivery time was approximately 2.0 milliseconds.

The prototype compensated pulsed alternator delivered 135 kilojoules to a flashlamp (resistive) load at a peak power of over 110 megawatts. A conventional rotating machine of similar dimension would be rated at approximately 6 MVA (steady state) at the same rotor speed.¹ The peak power developed by the conventional machine under short circuit conditions would be approximately 24 MW². Therefore, by using low reactance air gap windings and running at increased magnetic field strength, we have demonstrated the extension of the pulse power rating by a factor of four. To obtain this increase in performance, several potential manufacturing problems had to be addressed.

These included the following:

1. Development of a pre-loading technique to increase the effective modulus of elasticity of a laminated rotor to achieve subcritical operation at 5400 rpm.
2. Application of a phosphate conversion coating process to form an interlaminar insulating material with high electrical resistivity and low mechanical creepage at compressive loadings as high as 8.2×10^6 N/m² (1200 psi).
3. Application of an acid etching process to remove burrs formed during grinding of rotor surface.
4. Application of high strength glass filled pre-impregnated epoxy resin system to provide a high shear strength mechanical bond between air gap windings and steel rotor and stator.

5. Design of a compliant back iron support assembly to reduce the deceleration forces transmitted to the foundation.
6. Development of a serpentine air gap armature winding configuration.
7. Development of a new flashlamp triggering scheme and external pulse forming network to match the compulsator to the flashlamp load.

Conclusions

Based on the operation of the prototype compulsator test circuit and machine measurements made to date, we have reached the following conclusions:

1. The energy delivered to the load is increased as the minimum armature circuit inductance is reduced. The physical separation between the armature winding and the compensating winding should be reduced as far as mechanical and electrical limits allow.
2. The width of the output current pulse is controlled by the magnitude of the inductance variation as a function of rotor position. The maximum inductance of the engineering prototype is less than the original calculated value. The reduction in maximum inductance is due to a stronger effect of eddy currents in the solid pole faces than originally estimated.
3. Eddy current losses in the pole faces and in the end turn region of the rotor increase the effective resistance of the armature circuit dramatically when the windings are in uncompensated positions. The increase in effective armature resistance is the primary reason that the delivered energy is less than the design objective.
4. The Hecel F-155 epoxy resin system appears to provide an acceptable high strength bond between the air gap windings and smooth steel surfaces of the rotor and stator.
5. The single switch pulse power circuit developed at LLL performs lamp breakdown and alternator startup functions satisfactorily. The xenon flashlamps respond to the reverse current startup scheme as predicted by circuit simulation studies. When used as a switch, the flashlamps hold off the 6 kV bank voltage reliably and can be triggered consistently by pulsing the reflectors.
6. Connection of a current sharing reactor in series with each lamp provides proper current division among the parallel flashlamps.
7. A space harmonic distribution analysis has been developed to calculate the effective armature inductance as a function of rotor position. This method has been used to match actual

measurements made on the prototype machine. It appears that there is an optimum conductor span (fraction of pole pitch) that results in both minimum inductance at the compensated position and a maximum inductance variation ($L_{\max}/L_{\min}$) which is approximately the same as that used on the prototype.

Recommendations

Based on the test measurements and the requirement to increase the energy delivered to the load and to decrease the pulsewidth, we make the following recommendations:

1. Determine the tradeoff between increased minimum inductance and reduced inductance variation with the lower resistance obtained by maintaining conductor cross sectional area when reducing conductor span.
2. Investigate changing the winding distribution to increase the inductance variation in the vicinity of the minimum inductance position. Reconfigure the current distribution to emphasize higher harmonics.
3. Investigate the effect of laminating the solid steel pole faces to increase the maximum inductance to improve the flux compression ratio ($L_{\max}/L_{\min}$) and reduce coupled eddy current resistance.
4. Determine a method to reduce eddy current losses in the end turn region of the rotor. This includes such schemes as shielding the rotor laminations with a copper ring, replacing the steel laminations with a non-conductive material, and replacing the stainless steel end turn supports on the stator with glass banding.
5. Redesign the armature and compensating winding terminals to increase the radial clearance between the terminals and increase the breakdown voltage strength of the air gap.
6. Evaluate other insulating materials to serve as armature insulation on the air gap side of the compensating winding. This is necessary to replace the Kapton insulation that had insufficient peel strength to maintain dimensional stability after being wound in tension on the collapsible stator mandrel.
7. Evaluate the consequence (reduced performance) of increasing the mechanical clearance between the windings to increase the breakdown voltage of the air gap to 12 kV.

8. Determine the tradeoffs involved in holding voltage constant while scaling up machine dimensions. Improve voltage margin and flux compression factor at the expense of increasing current collection and switching problems associated with high machine current. (Run single lamps in parallel versus series pairs in parallel.)
9. Determine the susceptibility of the 12 x 7 x 30 AWG type 8 Litz wire construction to the formation of hot spots. Determine the tradeoffs involved in increasing basic wire diameter (increased eddy current losses versus lower probability of mechanical damage).
10. Design fault detection circuitry to open the series circuit breaker in the event of a fault at some time other than the time of the main discharge pulse. Evaluate the applicability of "smart" fuses to provide circuit protection.
11. Design forced cooling system for air gap windings.

Engineering Prototype Compulsator Test Log

Testing of the prototype compensated pulsed alternator was initiated September 26, 1979. The machine was motored to speeds ranging from 800 rpm to 5400 rpm more than 120 times. An abbreviated test log of these runs is given in Table 5-1. As can be seen from the remarks listed in the table, a total of 27 successful discharges into the flashlamp load were made. Other runs were made to perform mechanical tests prior to performing an electrical discharge test. Still more runs were made in a debugging mode as various startup problems were encountered and solved. These startup problems included the following:

1. Belt drive mounting and alignment
2. Noise sensitivity of Ross circuit breaker trip control
3. Noise sensitivity of rotor position and speed detection circuit
4. Noise sensitivity and loading of input registers of 8 channel timing chassis
5. Mechanical interference of G-10 brush debris collection rings
6. DC motor commutator flashover
7. Excitation of first torque frame critical frequency by DC motor drive.

At the time that high speed discharges were being performed, December 19 - December 20, 1979, the test system was performing reliably. The dc motor drive flashover problem had been corrected and the drive system was

successfully bringing the rotor to full speed. The rotor position and speed sensing circuit was performing reliably and all system controls and trigger circuits were working properly.

TABLE 5-1

ENGINEERING PROTOTYPE TESTS

Test Number	Date	RPM	Remarks
1	9/26/79	800	Coast down. Breakaway torque 10 lb-ft
2	9/26/79	1500	Coast down. Shear pins failed
3	9/28/79	1000	Dynamic braked
4	9/28/79	1000	Energized field
5	10/1/79	1900	Shear pins failed
6	10/1/79	3760	Dynamic braked. Indication of G-10 interference
7	10/2/79	1000	Cycled brushes. Dynamic braked
8	10/2/79	1000	Open circuit voltage test
9	10/3/79	1500	Dynamic braked. System check
10	10/3/79	1250	Checked discharge sequence
11	10/3/79	1500	Checked discharge sequence. Fired PILC circuit
12	10/3/79	1500	Discharge sequence. No trigger
13	10/3/79	1500	Discharge sequence. Trigger OK
14	10/3/79	1500	Discharge sequence. Trigger OK, phase error
15	10/3/79	1500	Discharge sequence. Dynamic brake. Controller not reset
16	10/3/79	1500	Discharge sequence. Dynamic brake. Field breaker open.
17	10/3/79	1500	PILC OK. Mechanical phase measurement signal noise sensitive.
18	10/3/79	1500	Shear pins failed
19	10/3/79	1500	Ross breaker failed to close
20	10/4/79	1500	Discharge sequence. No PILC
21	10/4/79	1500	Discharge sequence. PILC OK
22	10/4/79	1500	Discharge sequence. PILC OK
23	10/4/79	1400	Shear pins failed
24	10/4/79	1500	Ross breaker failed to close
25	10/4/79	1500	Ross breaker failed to close
26	10/4/79	1500	Discharge sequence. Premature trigger
27	10/4/79	1500	Discharge sequence. Premature trigger
28	10/4/79	1500	Discharge sequence. Premature trigger
29	10/4/79	1000	Shear pins failed
30	10/4/79	1500	Successful discharge into flashlamps. 170 μ f @ 5W
30b	10/4/79	1300	Premature trigger
31	10/4/79	1900	Successful discharge into flashlamps. 680 μ f @ 5.5W
32	10/4/79	2700	Premature trigger
33	10/5/79	2500	Ross breaker did not close

TABLE 1 CONTINUED
ENGINEERING PROTOTYPE TESTS

Test Number	Date	RPM	Remarks
34	10/5/79	1850	Discharged. Triggered 30 mechanical degrees early
35	10/5/79	1800	Ross breaker did not close
36	10/5/79	1100	Shear pins failed
37	10/5/79	2500	Ross breaker did not close
37a	10/5/79	1667	Premature trigger during motoring
38	10/5/79	2500	Ross breakers did not close
39	10/5/79	1285	Premature trigger with brushes raised
40	10/5/79	1900	Ross breaker did not close
41	10/9/79	1200	Instrumentation check
42	10/10/79	2400	Mechanical transducer check
43	10/11/79	1800	PILC OK
44	10/11/79	1800	PILC OK. Phase angle error
45	10/11/79	1800	PILC OK. Phase angle 8° early
46	10/11/79	1200	Premature trigger
47	10/11/79	1800	PILC OK. Mechanical angle early
48	10/11/79	1800	PILC OK
49	10/11/79	1800	PILC OK. Premature scope trigger
50	10/11/79	1800	PILC OK after reset. Premature trigger during motoring
51	10/11/79	1800	PILC OK. No prefires
52	10/11/79	1800	Successful discharge after reset. (Had premature scope trigger and ross breaker trip)
53	10/11/79	1800	Premature trigger
54	10/12/79	1800	Successful discharge into flashlamps
55	10/12/79	2400	Successful discharge into flashlamps
56	10/12/79	2000	Abort due to PILC pre-fire
57	10/12/79	2000	Abort due to PILC pre-fire
58	10/12/79	2000	Abort due to PILC pre-fire
59	10/12/79	2000	Abort due to PILC pre-fire at time bank charged to full voltage
60	10/12/79	2400	Successful discharge into flashlamps
61	10/12/79	3000	Premature trigger
62	10/12/79	3000	Successful discharge, but early mechanical angle. Strong 6-10 rub.
63	10/15/79	1800	Cycled brushes to check for interference

TABLE 1 CONTINUED

ENGINEERING PROTOTYPE TESTS

Test Number	Date	RPM	Remarks
64	10/15/79	2400	Shear pins failed
65	10/15/79	3000	Cycled brushes
66	10/15/79	1800	Checked new logic for mechanical angle signal
67	10/16/79	3000	Checked new mechanical angle signal
68	10/17/79	1800	Discharged successfully into flashlamps
69	10/17/79	2400	Discharged successfully into flashlamps
70	10/17/79	2400	Discharged successfully into flashlamps
71	10/17/79	2400	Aborted run. No mechanical angle signal
72	10/17/79	2400	Discharged successfully. Observed by pass resistor current
73	10/17/79	3000	Discharged successfully. Noticed rotation of back iron as predicted
74	10/18/79	1800	Checked drive mechanism after realignment
75	10/18/79	3000	PILC OK. Timing OK
76	10/18/79	3000	Successful discharge into flashlamps
77	10/18/79	2800	Abort. No brush air supply
78	10/18/79	3000	Successful discharge into flashlamps
79	10/18/79	3600	Successful discharge into flashlamps
80	10/18/79	3600	Successful discharge into flashlamps
81	10/18/79	3600	Successful discharge into flashlamps
82	10/19/79	3600	Successful discharge into flashlamps
83	10/19/79	3600	Same as 82 but without alternator field
84	10/19/79	3600	1700 μ f @ 5.5kV. Successful discharge into flashlamps
85	10/19/79	3750	1700 μ f @ 6.0kV. Successful discharge into flashlamps
86	11/12/79	2000	Coast down. Indication of G-10 interference at top brush assembly
87	11/13/79	1900	Actuated brushgear for 10 seconds. Indication of G-10 interference
88	11/13/79	2300	Checked timing sequence. Coast down - still have G-10 rub
89	11/13/79	3000	Checked rotor displacement and timing. No trace of G-10 rub
90	11/13/79	3200	Checked rotor displacement and timing. G-10 interference
91	11/13/79	3700	Checked rotor displacement and timing. G-10 interference
92	11/14/79	4300	Checked rotor displacement and timing. No indication of G-10 interference. Actuated brushes at 3600 rpm for 15 seconds
93	11/14/79	2400	Attempted to discharge into dummy load at 45 mechanical degrees. Anode voltage too small for ignitron to trigger

TABLE 1 CONTINUED
ENGINEERING PROTOTYPE TESTS

Test Number	Date	RPM	Remarks
94	11/14/79	3700	Successful discharge into flashlamp load. 700 μ f @ 6kV. Growing large amplitude displacement of top balancing ring. Later welded upper balance ring to shaft
95	11/15/79	3800	Checked displacement transducer signal after welding upper balance ring to shaft. Dynamic braked
96	11/15/79	4200	Commutator flashover on DC motor drive
97	11/16/79	2150*	*Motor rpm (belt disconnected). Adjusted motor armature current limit
98	11/16/79	3800	Checked motor drive under load.
99	11/16/79	4500	Checked motor drive under load. Dynamic braked
100	11/19/79	4300	Added trial balance weight. Measured rotor displacements
101	11/19/79	4760	Checked rotor displacement
102	11/20/79	4000	Flashover - DC motor commutator. Reseated brushes. Installed forced ventilation blower.
103	11/21/79	4700	Added balancing weights. Measured acceleration of back iron. DC motor exciting first critical frequency of compliant mounting assembly.
104	11/26/79	4200	Measured acceleration of torque frame and back iron, and relative displacement of torque frame to back iron.
105	11/27/79	4000	Flashover - DC motor commutator. Shimmed brush boxes to eliminate excessive clearance. Reseated brushes.
106	11/29/79	3700	Sheared shear pins on drive coupling
107	11/30/79	4300	Flashover - DC motor commutator. Reworked motor drive system as follows: Changed taps on isolation transformer to reduce AC voltage 5%. Covered lower motor air vents with plexiglass plates. Realigned brush boxes on mounting ring. Reseated brushes. Aligned motor jackshaft and chain coupling. Increased field current by decreasing series resistance.
108	12/11/79	4550	Measured 6 mil peak to peak displacement, shaft to bearing housing. DC motor commutator did not flashover. Acceleration rate possibly reduced above 4000 rpm. Decided to run at higher initial oil temperature (oil viscosity $\mu_{90^{\circ}\text{F}} = 2.5\mu_{120^{\circ}\text{F}}$) to reduce rotational losses. Installed stiffening arm in belt drive mounting system.

TABLE 1 CONTINUED
ENGINEERING PROTOTYPE TESTS

Test Number	Date	RPN	Remarks
109	12/12/79	2000	Shear pins failed during motoring. Shimmed stiffening arm mounting flange and realigned motor jackshaft and chain coupling
110	12/12/79	4600	Measured 6.2 mils peak to peak displacement, shaft to bearing housing
111	12/12/79	4900	Measured 6.5 mil peak to peak displacement, shaft to bearing housing. (Note - this measurement includes mechanical runout.)
112	12/13/79	5400	First full speed mechanical run. Coast down
113	12/19/79	2400	Circuit test. Flashlamps did not trigger. No power to trigger chassis
114	12/19/79	2400	Successful PILC test
115	12/19/79	2400	Attempted discharge. Ross Breaker closed late
116	12/19/79	2400	Successful discharge into flashlamps. 700 μ f @ 6kV
117	12/19/79	4200	Premature PILC and ignitron trigger
118	12/19/79	4200	Late PILC and ignitron trigger. Determined that LLL 8 channel timing chassis delay timer settings are not loaded until next input trigger pulse. Need to give a false trigger signal to load registers prior to discharge trigger signal
119	12/19/79	4200	Successful discharge into flashlamps. 700 μ f @ 6kV
120	12/20/79	4800	Successful discharge into flashlamps. 700 μ f @ 6kV (low alternator field current)
121	12/20/79	4800	Successful discharge into flashlamps. 700 μ f @ 6kV. 112 MW to flashlamp load. Recorded 0.16 cm rotation of the back iron.
122	12/20/79	5400	Internal fault of alternator. Recorded 0.8 cm rotation of the back iron.

REFERENCES

1. L. A. Kilgore, personal communication.

APPENDIX A. PROTOTYPE COMPULSATOR TEST RUN NUMBER 122

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APPENDIX A PROTOTYPE COMPULSATOR TEST RUN NUMBER 122

Test History

The compensated pulsed alternator test program was arranged to perform mechanical and electrical tests progressively, beginning at low rotor speeds and energy, and working up to the maximum design speed of 5400 rpm in 600 rpm increments. Electrical discharge tests were to be made at each speed before running mechanical tests to the next highest speed. The test program was begun September 26, 1979. The rotor was accelerated to full speed for the first time during Test Run Number 112 on December 13, 1979. The final set of high speed discharge tests was initiated December 19, 1979. The prototype machine was successfully discharged at 2400 rpm (Run Number 116) to check the control and instrumentation system. The machine was then discharged at 4200 rpm (Run Number 119). The following day, December 20, 1979, the Compulsator was discharged from 4800 rpm on two occasions. (Run Numbers 120 and 121.) At that time the system was well debugged and performing reliably. It was decided to discharge the machine from 5400 rpm. During Run Number 122, after the rotor was accelerated to full speed, the field energized, the brushes actuated, and the Ross circuit breaker closed, a short circuit developed between the high voltage rotor terminal and the grounded stator terminal. It appears that the fault began at the upper end of the rotor beneath the upper brush ring assembly.

Short Circuit Waveforms

The alternator current was measured using two Pearson Model 301-X Pulse Current Transformers, one in each of two legs of the Ross Vacuum Circuit breaker. A photograph of an oscilloscope trace of the output of one of the current transformers is shown in Figure A-1. The top two traces in Figure A-1 are flashlamp voltage and current, respectively. The bottom trace is the alternator current flowing in one leg of the vacuum interrupter (10 kA/div, 500 μ sec/div). Based on this trace the first peak of the short circuit current is over 36 kiloamps. The current cleared at the first zero crossing, but restruct 2.0 milliseconds later.

The alternator terminal voltage was displayed on another oscilloscope that did not trigger at the instant of the fault. However, the voltage signal was recorded on the 28 channel FM tape recorder. A photograph of a

Figure A-1. Flashlamp Voltage, Flashlamp Current, and One-Half Alternator Current Oscilloscope Traces - Run Number 122.

Nicolet digital oscilloscope trace of machine voltage, made from the tape playback is shown in Figure A-2. The peak to peak voltage is approximately 13 kV. The period of the waveform just prior to the fault is 5.56 milliseconds. The voltage signal was also played back onto the Honeywell Visicorder (light beam oscillograph). The voltage trace over a longer time period is shown in Figure A-3.

The fault occurred at the instant of peak positive voltage (positive toward the bottom of the photograph). The peak voltage is at least 5.8 kilovolts, and is possibly as high as 6.5 kilovolts. There is some question concerning the voltage level because of the possibility that a Fluke digital multimeter, which was used to calibrate the high voltage probes, was in error.

It appears that the second positive going current pulse is larger in amplitude than the first peak because of the higher arc voltage and because of the shape of the current pulse seen at the extreme right of the oscilloscope trace shown in Figure A-1. This is likely if the armature winding linked a larger fraction of the applied field flux at the time of restrike. There is further evidence that the main field flux was compressed during the short circuit. A fast acting fuse in the primary of the field power circuit opened during the fault, indicating that the field circuit experienced a transient overcurrent.

Tachometer signals were lost during the first stages of the fault. However, the average speed over one revolution could be determined from the top North-South displacement transducer, which is placed radially opposite the upper balancing ring. After ten milliseconds the rotor had decelerated from 5450 rpm to 4615 rpm, reducing the energy stored inertially by 990 kilojoules. After 40 milliseconds over 1.8 megajoules had been removed from the inertial energy store and fed into the fault.

After this time, the terminal voltage began to appear sinusoidal with higher frequency components superimposed on the fundamental. The peak to peak voltage began to increase with time and the amplitudes of the higher harmonics began to reduce. After 200 milliseconds the rotor speed stabilized at 3600 rpm and the terminal voltage reached three kilovolts peak. This is the voltage that one would expect if the field power supply were operating in a single-phase mode after the fuse in Phase A of the primary had cleared.

Figure A-2. Alternator Terminal Voltage (Nicolet Oscilloscope) -
Run Number 122.

PROTOTYPE COMPULSATOR RUN NUMBER 122
MACHINE TERMINAL VOLTAGE

Figure A-3. Alternator Terminal Voltage (Honeywell Visicorder) -
 Run Number 122.

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After 920 milliseconds the rotor had decelerated to 3430 rpm. At that time the terminal voltage again collapsed and contained higher frequency components that were indicative of arcing. At a time approximately 1000 milliseconds into the fault, the second Tektronix 5403 oscilloscope triggered. A photograph of terminal voltage (top trace 5 kV/div), one half Ross breaker current (middle trace 10 kA/div), and output terminal current (bottom trace - 17.5 kA/div) oscilloscope traces (1 msec/div) is shown in Figure A-4. The Ross breaker current was measured with a Pearson Model 301-X pulse current transformer, and the output terminal current was measured with a Rogowski coil manufactured by the Center for Electromechanics. The total peak current at this time was approximately 18 kiloamps.

Finally, after 1020 milliseconds the field power supply contactor was opened automatically and the applied field flux began to decay exponentially. The short circuit currents probably continued to flow intermittently until the flux decayed to remanence. After three seconds approximately 90 percent of the energy originally stored in the rotating inertial had been dissipated.

Machine Damage Status

On December 21, 1979 disassembly of the compensated pulsed alternator was initiated to determine the extent of damage that was incurred during the final test run.

The prototype Compulsator did withstand the high speed maximum voltage fault without causing serious damage to auxiliary components and without causing any injury to personnel. The compliant mounting assembly allowed the back iron to rotate approximately 0.8 cm at the point of support. This reduced the loading placed on mounting bolts tied to the foundation so that the back iron-torque frame assembly remained mechanically stable.

The primary damage to the machine was that holes were burned in both the rotor and compensating windings at several points along the axial length of the machine. A photo-mosaic of the developed rotor surface is shown in Figure A-5. The photographs were taken with the rotor mounted in a lathe, after removing the outer fiberglass bonding wrap. The locations of these spots are given in Table A-1. It appears that similar holes are present in the compensating winding. It will be necessary to rewind both the armature and the compensating windings before it will be possible to resume discharge tests.

Figure A-4. Alternator Terminal Voltage, One-Half Vacuum Breaker Current, and Output Terminal Current Oscilloscope Traces - Run Number 122.

TABLE A-1
LOCATION OF ROTOR HOT SPOTS

Location (Ranked in order of severity)		Conductors Involved (1-47)
1	Bottom-end turns	6, 10, 14
2	Middle-active region	34, 38
3	Top-inside corner of end turn	2
4	Top-outside corner of end turn	45
5	Top-end turns	26, 30
6	Top-end turns	39, 45

Figure A-5. Photo-mosaic of Rotor Winding after Fault

Other damage to the machine included slight pitting of the upper journal bearing Babbitt liner, mechanical and electrical burning of the G-10 pieces of the brush and slip ring assembly, and failure of bolts which mount the brush assemblies to the stator.

Cause of Terminal Fault

The fault occurred at the instant of peak positive going voltage when the compulsator rotor was near the position of minimum inductance and the interior segments of the output terminals were in close proximity. The fault occurred approximately three seconds after closing the field power supply circuit breaker and approximately 500 milliseconds prior to triggering the series ignitron.

After disassembly of the brush mechanism, it was seen that a G-10 ring, used to prevent brush wear debris from reaching the main air gap between the rotor and stator windings had become loose. Also, the copper slip ring for the corona suppression brushes had also become loose. Both pieces are at the top of the rotor. (See Figure A-6.) Mechanical wear debris from this material could have abraded the surfaces between joints of insulation and caused flashover between the solid copper rings which terminate the Litz wire conductors. This arc then extinguished as the arc length was increased due to rotation of the rotor, or by magnetic forces blowing the arc out toward the exterior terminals. Successive restrikes resulted in additional energy being deposited in the windings.

From first inspection it appears that the Litz wire may have been damaged, either during original manufacture, or during construction of the windings. Local heating at these spots resulted in a thermal runaway condition, which eventually led to turn to turn failures. This arcing could eventually have led to arcs developing across the gap between windings. Eventually, enough material gathered at points in the gap, coupled with thermal expansion, to result in mechanical interference between the rotor and stator bore.

The problem could have been aggravated by other factors. First, the machine was overexcited by 25 percent. This was necessary in part to overcome the reduction in radial flux density near the ends of the poles due to axial leakage flux. The net excitation was approximately 12 percent higher than the design level. This accounted for the higher level of the open

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Figure A-6. Sketch of Compulsator - Location of Fault.

circuit voltage at 5400 rpm (6.5 kV peak versus 5.8 kV peak). This over-voltage could have been avoided by either reducing the secondary tap to 75 percent or changing the field timing circuit. The latter would have been preferable, since reducing the voltage taps would have reduced the open circuit voltage to less than 5.2 kV. Second, if the corona suppression system brushes had failed, the majority of the dielectric stress would have been placed on the air gap. The gap could then have broken down. The corona or arc could then have possibly found a weak point in the solid dielectric or caused flashover along a cracked surface near the output terminals. Such a crack could occur if a G-10 ring, bonded to the rotor, loosened due to the increased spin stress at higher speeds. This G-10 ring is used to insulate the copper rotor output terminal.

References

1. Rudenberg, Reinhold, Transient Performance of Electric Power Systems, Phenomena in Lumped Networks, M.I.T. Press, Cambridge, Mass., 1970, pp. 147-161.

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ERRATA SHEET

3/19/80

Page No. Correction

Abstract Change line 10 to read as follows: "... 6 kV, 180 Hertz, at a ..."

77 Change "Dimension" of "Parameter R'_S" in Table 3-5 to read as follows: "21.85"

77 Add the following to Table 3-5:

<u>Parameter</u>	<u>Definition</u>	<u>Dimension</u>
ℓ	Effective Length (cm)	124

96 Change title of Figure 4-39 to read as follows:

"Equivalent Thermal Circuit for Curing Rotor Ground Plane Insulation."

124 Change line 8 of second paragraph to read as follows:

"...was checked by lowering the shaft..."

131 Change first sentence of last paragraph to read as follows:

"The thrust bearing housing is bolted to the lower journal bearing housing, so that the weight of the rotor..."

138 Change designation of first (left) column of TABLE 4-7 to read as follows: "Wire Size (AWG)."

139 Change line 5 of last paragraph to read as follows:

"...system, S-Y Bondeze, also manufactured by..."

142 Change line 4 of first paragraph to read as follows:

"...wire insulation) is 12 kV/cm (30.4 VPM)..."

146 Change line 3 of first complete paragraph to read as follows:

"...nominal dielectric stress, therefore, is 18 kV/cm..."

172 Change third from last line on page to read as follows:

"...torque applied at the shaft..."

183 Change first sentence in Recommendation No. 2 to read as follows:

"...Investigate changing the winding distribution..."