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EVALUATION AND ANALYSIS OF MOTOR STARTING PROBLEMS AT FOUNTAIN VALLEY PUMPING PLANT

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16. ABSTRACT

Report consists of an evaluation and analysis on the motor startup problems at Fountain Valley Pumping Plant. Included is an analysis of test data obtained from both the contractor and subcontractor in addition to Bureau data collected on three separate occasions.

17. KEY WORDS AND DOCUMENT ANALYSIS

a. *DESCRIPTORS*--/filter capacitors/ feedback transformers/ Fourier analysis/ asymetrical inverter/ harmonics/ harmonic distortion/ inverter harmonics/ motor starting/ short circuit ratio

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EVALUATION AND ANALYSIS OF MOTOR STARTING PROBLEMS AT FOUNTAIN VALLEY PUMPING PLANT

by

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March 1984

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SI METRIC

UNITED STATES DEPARTMENT OF THE INTERIOR ★ BUREAU OF RECLAMATION

As the Nation's principal conservation agency, the Department of the Interior has responsibility for most of our nationally owned public lands and natural resources. This includes fostering the wisest use of our land and water resources, protecting our fish and wildlife, preserving the environmental and cultural values of our national parks and historical places, and providing for the enjoyment of life through outdoor recreation. The Department assesses our energy and mineral resources and works to assure that their development is in the best interests of all our people. The Department also has a major responsibility for American Indian reservation communities and for people who live in Island Territories under U.S. Administration.

CONTENTS

Glossary of Abbreviations and Symbols

A	ampere
a-c or a.c.	alternating current
CT	current transformer
d-c or d.c.	direct current
DVM	digital voltmeter
H	henry
μH	microhenry
hp	horsepower
Hz	hertz
I	current
PF	power factor
p-p	peak-to-peak
PT	potential transformer
pu	per unit
Q	volt-amperes reactive
kQ	kilovolt-amperes reactive
ROC	rotor overcurrent
rms	root mean square
SCR	short circuit ratio
V	volts
kV	kilovolts
kV·A	kilovolt-amperes
MV·A	megavolt-amperes
V_s	system voltage
Z	impedance

INTRODUCTION

Fountain Valley Pumping Plant No. 1 has four 700-hp, wound-rotor, induction motors fitted with slip loss and energy recovery systems manufactured by Anvic International. The slip loss recovery system essentially consists of a three-phase rectifier bridge and an inverter bridge. The rectifier converts rotor slip frequency a-c voltage to d.c. The inverter converts the d.c. to a.c. for power feedback into a 60-Hz, a-c system. In this scheme, the motor is able to operate at various speeds with substantially higher efficiency than a conventional, wound-rotor, variable-speed, induction motor with adjustable rotor resistors. Obviously, feeding rotor power back into the system is more efficient than dissipating it in the rotor circuit resistors. The conventional and slip recovery systems are shown on figures 1 and 2 in appendix A.

CONCLUSIONS

The startup problems at Fountain Valley are due to an inadequate SCR (short circuit ratio) at the drive side of the feedback transformer. The rather high impedance of the feedback transformer is the predominate factor in determining the SCR and, in fact, has limited the SCR to an unacceptably low value. The SCR and percent distortion factors should have been considered in the sizing of the feedback transformer. Neglecting this equipment coordination task resulted in the manufacturer selecting a feedback transformer considerably smaller than actually required for proper operation.

An insertion resistor scheme during the initial startup period has proved successful in eliminating the startup problems. Insertion resistor starting eliminates the problem by reducing the SCR requirements of the drive.

ANALYSIS OF STARTUP INVESTIGATION

Harmonic Terminology

Throughout this report the terms "harmonic" and "harmonic like" have been used. These harmonic terms are normally derived from a Fourier analysis and/or transform of the complex waveform. The term "harmonic" refers to an integer multiple sine wave of the fundamental component of a complex and periodic waveform. The term "harmonic like" is used to indicate that the distorted or complex waveform, although not of a steady state nature, is somewhat sustained and sufficiently repetitive so as to approximate a periodic waveform. In this respect, a Fourier analysis can be performed and "harmonic like" components can be obtained. The period of sustained complex repetitive waveforms varied from several tenths of a second to more than several seconds. There were also test results involving transient related distortions where the subject waveforms changed drastically from one cycle to the next. In these instances, references to Fourier and harmonic terminology were avoided.

Background

Prior to November 1982, only one pump at a time had been started and run at Fountain Valley Pumping Plant No. 1. On November 5, 1982, a representative of the manufacturer attempted to start a second unit while the first unit was at operating speed. Shortly after initiating the startup sequence, the first unit tripped off-line. Protective equipment targets indicated the trip was due to overload conditions. Additional attempts to start a unit while another unit was on-line resulted in an identical trip sequence.

Analysis by Manufacturer

A series of tests were performed in November 1982 in an attempt to determine the nature of the startup problem. After review of the test data by the manufacturer and the manufacturer's consultant, they concluded in a letter to the Bureau (app. B) that the equipment was not at fault and stated the problem was related to the 2400-V power supply system.

In the preliminary report (app. B), the manufacturer stated the mode of shutdown of the running unit was ". . . determined to be misfiring of the silicon-controlled rectifiers due to voltage distortions on the 600-V lines inside the Econodrive." This misfiring resulted in short circuits through the silicon-controlled rectifier bridge. The report goes on to say the voltage distortion was due to higher than expected current draw from the silicon-controlled rectifier (power feedback) transformer, which upset the voltage stability in the starting motor circuits. In turn, this produced (by means of cross talk) misfiring, due to waveform distortion, in the running drive and motor. The final conclusion by the manufacturer was that ". . . the cross talk is due to the inability of the 2300-V lines to maintain stability when called upon to provide the inrush current required upon immediate static connection of a complete drive system." Although this is not a totally wrong statement, it is somewhat misleading in that the manufacturer did not address the problem. Instead, attention was directed on an effect rather than on the cause of the problem. The proposal to stiffen the bus is an expensive brute force method that may or may not prevent inadvertent running drive trips and would not cure the underlying phenomenon that created the problem.

Our analysis of the manufacturer's data was based on uncalibrated oscillograms, and all the data was submitted without scales and values assigned. These omissions prevented a rigorous analysis of the oscillograms.

After receiving the manufacturer's preliminary report and copies of the test oscillograms, the Bureau's Division of Design requested the Power and Instrumentation Branch to analyze the data and provide comments. For details of our response to this request, please refer to appendix C. Also included in appendix C are several of the submitted test records and oscillograms. The Bureau's review of the data resulted in either of the two following conclusions:

1. Energizing the power feedback transformer, harmonic filter, and associated equipment simultaneously resulted in the oscillation of a tuned circuit with an extremely high quality factor centered around 180 Hz, the third harmonic frequency.

2. The saturation and inrush current of the transformers combined with the high quality factor of the near third harmonic filter may have resulted in sustained harmonic or harmonic like oscillations due to transformer ferroresonance effects.

On December 3, 1982, a meeting was held with the manufacturer, the contractor, and the involved Bureau offices. The manufacturer stated the startup problem was due to the weak power system, whereas the Bureau defended the two possible conclusions previously mentioned. The prime contractor stated that he would hire a power system consultant to review the data. It was noted at the time that the manufacturer's previous consultant was a solid-state motor drive expert and not a power system engineer.

Discussion of Consultant's Preliminary Evaluation

In late February 1983, we were informed that the consultant hired by the prime contractor had essentially agreed with the Bureau as to the cause of the problems at Fountain Valley. As a result of the consultant's analysis and report, the manufacturer and the consultant decided to make modifications to the circuits that would hopefully eliminate the sustained harmonic oscillations, and then perform field tests to verify the results. After temporary modifications had been made, it was found that a second unit could be started and run without affecting another running unit. However, upon starting a third unit, there was still unit tripping due to undervoltage relay operations. Additional tests led the consultant to believe the system was considerably weaker than required, and sustained undervoltages of about 8 percent could be expected.

At this time, it was generally agreed that there were two distinct points of view regarding the startup problems: (1) sustained harmonic like oscillations, and (2) a weaker than required system. It should be noted that these two possibilities are separate and that one is not causing the other. In fact, the converse is true to some degree in that a weaker system will tend to limit the harmonic or harmonic like oscillations. During ferroresonance conditions, as the harmonic current increases, the weak system voltage will sag and limit the peak voltage applied across the power feedback transformer. This will limit transformer current to a larger degree because, in the region of saturation, small increases in voltage will result in much larger increases in current. While it is true that a stiffer system would reduce the cross coupling between drives, it would also contribute to the sustained oscillations on the drive being started. The Bureau believes that the sustained harmonic overcurrents are a very severe duty to impose on the transformers, harmonic filters, and associated equipment. The Bureau also finds it difficult to accept that the system is weak enough to sustain an 8 percent or more voltage drop during startup, and the running of a motor that has no inrush current. These induction motors and drives come up to speed under controlled conditions so that the normal 5 to 6 per unit starting current is reduced to 1, or less, per unit. Based on a 2300-V supply, the line current for a 700-hp pump is about 130 A. From previous oscillograms, there is negligible voltage dip when starting one motor, so why should there suddenly be an 8 percent drop only when starting the third motor.

Discussion of Consultant's Final Evaluation and Report

The consultant to the prime contractor performed additional field tests on February 22 and 23, 1983, and the results reported in "Variable Speed Drive System Analysis and Test Report," dated March 18, 1983. Appendix D consists of detailed comments on this report along with several reproductions of oscillograms which were part of the report. The consultant made two recommendations:

1. The power factor capacitors should be relcoated.

2. The Southern Colorado Power Co. should provide an accurate assessment of the current system short circuit capability.

At the time, we found nothing wrong with these recommendations and, if the problem at Fountain Valley was transient related, moving the capacitors may actually have eliminated the problem. The consultant also made several statements in his report that implied the inadvertant tripping of units was due to a weak system. Being specific, with respect to test 5-2B, the report states, ". . . this failure was due to a severe voltage drop on the 2300-V line in the order of magnitude of 10 percent or greater." We disagree with this statement and believe the data presented in the consultant's report indicate the severe voltage drop was due to an inverter silicon-controlled rectifier misoperation that applied a phase-to-phase fault on the 600-V bus, thereby reducing the system voltage.

Our analysis of the February tests was based only on the test data and oscillograms presented in the consultant's report. This data, as was the case with the November 1982 data, lacked the scale and magnitude data required for a rigorous analysis. A detailed report of our analysis, on the February tests is presented in appendix D. In general, we disagree with the consultant's observations. Our analysis of the test oscillograms strongly supports the contention that the system is sufficiently strong and that the problem is equipment related. The general conclusion of our analysis is that it appears that the silicon-controlled rectifier drives misfire, causing phase-to-phase faults more often than not when starting other units. These faults may be due to:

1. Transients (high and/or low frequency in nature)

2. Ferroresonance (while switching)

3. Tuned circuit oscillations (induced while switching)

4. Improperly placed power factor correction capacitors (as suggested by the consultant; i.e., transients)

5. Improperly selected feedback transformers (i.e., the large transformer magnetizing inrush alone may distort the voltages enough to misfire the silicon-controlled rectifiers)

6. Feedback circuit design related (i.e., a delta on the filter side may help improve the 600-V system waveforms more than a delta on the line side of the transformer)

RELATED PROBLEMS AND POSSIBLE SOLUTIONS

A brief, informal literature search has produced two IEEE papers (app. E) on the subject of transformer inrush currents in conjunction with a-c harmonic filter circuits. Both of these papers relate to problems encountered in the design of high-voltage, d-c converter stations for transmission of power by d-c methods. The papers are very relevant to Fountain Valley in that a d-c converter station consists of an inverter to convert the d-c power to a.c., filters for power factor correction and harmonic suppression, and switched transformer banks between the a-c system and filters. This configuration is exactly that of Fountain Valley and, in many respects, the Econodrive can be considered as a miniature d-c converter terminal.

The Sakurai paper states ". . . closing the converter transformer onto the a-c system can cause temporary overvoltages or voltage distortions of duration up to about 1 second due to the converter transformer magnetizing inrush current containing low harmonics and the resonance of the a-c filter bank with the a-c system." This statement sounds very familiar. One of the conclusions of the paper is that without a damping resistor suppressor, magnetizing inrush currents would necessitate highly oversized capacitors in the fifth harmonic filter.

The second paper, by Thio, essentially states the same problem – that transformer inrush currents excite the third and fourth harmonics and can produce highly distorted a-c voltages. The solution presented in this paper was also to install preinsertion resistors which decrease magnetizing inrush currents by effectively removing the voltage from the transformer nonlinear inductance when it reaches the saturation region (during peaking portions) of the current waveform. Studies have indicated that fairly high resistors could be used with insertion times of about 5 to 15 milliseconds.

Based on the literature search, the use of preinsertion resistors may be the easiest and most direct method of eliminating the inrush related problems at Fountain Valley. An inexpensive, low-current auxiliary contactor and resistors installed around the main contactor are worth further investigation as a possible solution.

SYSTEM CAPACITY INVESTIGATION

Because of the difference of opinion regarding the source of the problem at Fountain Valley, and the urgency involved in completing the plant, it was decided and agreed by all parties involved to perform a system impedance test. This test would definitely determine if system capacity was in fact the real problem. The test was scheduled for June 6 and 7, 1983. To obtain the impedance data, it was agreed that the data could be obtained by starting one of the motors as a conventional induction motor (rotor short circuited). It was also agreed that standard current and voltage transducers of the 0.1-percent accuracy class, connected to a laboratory grade strip chart recorder, would be sufficient for measuring the system parameters. This equipment was augmented with a laboratory grade oscillograph. Although less accurate than the strip chart recorder, the oscillograph would provide valuable information on current and voltage waveshape and offset. The data from these tests are presented in appendix F.

In summary, the system impedance test results showed that the 13.8-kV system short circuit megavolt-ampere capacity was 55.4 MV·A, which was about 13 percent greater than the 49 MV·A value used by the Bureau during the final design. Short circuit capacity during the motor startup transient period was about the same as the steady-state value.

The 13.8-kV system voltage drop was 7.2 percent when a 960-A predominately reactive load was applied to the 2400-V bus. The voltage drop on the bus was 17 percent. Based on the system impedance calculated from the test, the system voltage drop for 1 per unit rated motor current at 0.9 power factor was 2.7 percent. This is the system (impedance) voltage drop, not the voltage drop at the motor terminals. The motor terminal voltage drop is only a fraction of this value and has been calculated, from the test data, to be about a 1.2 percent voltage drop per motor at rated load and 0.9 power factor. This agrees very well with the average 1.3 percent voltage rise observed on the strip chart records whenever a loaded unit tripped off-line. Other system measurements from the tests indicated the actual voltage drop at the motor terminals to be from 1.3 to 1.5 percent. The reason that the system voltage drop is not equal to the motor voltage drop is because the system impedance is primarily reactive, whereas the motor load is mostly resistive. Therefore, the system reactance voltage drop is out of phase with the motor voltage by more than 60 degrees and, as a result, the motor voltage drop is smaller and must be calculated using vector analysis.

The oscillograph records show that the 2400-V bus voltage drops 4 percent when the feedback transformer, motor stator, and filter circuit are energized. This voltage drop is due to the circuit transient inrush current. Because we now know that each unit results in about a 1.5 percent voltage drop at the motor terminals, it becomes obvious that the worst case unit voltage drop with two units running while starting a third unit is about 7 percent. This is considerably less than the ± 10 percent line voltage variation specified by both the Bureau specifications (IEEE requirements) and the drive manufacturer requirements. All of the data strongly support the Bureau's contention that the system capacity is adequate and meets the plant requirements.

The 20-to-1 SCR required by the drive manufacturer is defined by the IEEE Standard 519-1981 on "Harmonic Control" as the system short-circuit, circuit capacity in megavolt amperes divided by the converter capacity in megawatts. The Fountain Valley SCR has been calculated to be 16 on the converter side of the feedback transformer. This ratio is rather inadequate based on both the IEEE standards and on the drive manufacturer's specifications. The SCR based only on system impedance (assuming a zero impedance feedback transformer) is 117. The SCR of the feedback transformer only, assuming a zero impedance system, is 18.8. It is now clear that the feedback transformer impedance is the limiting factor in determining the SCR. Reducing the feedback transformer impedance by 50 percent or increasing the feedback transformer megavolt-ampere rating by 100 percent would result in an overall SCR of 28.5. This would result in a more than adequate SCR by both the IEEE guidelines and the drive manufacturer's requirements. The SCR is referred to in the IEEE standards primarily for estimating the percent distortion factor, which is a means of determining the effects of harmonics on the power system voltage waveforms. Apparently, the drive manufacturer neglected to consider the percent distortion factor and SCR in selecting the feedback transformer.

In addition to the information previously presented, it was discovered that starting one unit in an "as supplied" configuration resulted in a peak inrush current of about 4.8 to 6.2 per unit. This is considerably greater than the 1 per unit maximum starting current specified by the manufacturer in his literature. This large difference would result in errors in the required SCR and seriously impact the plant integration and equipment specifications.

MOTOR STARTUP TESTS

The purpose of the June 15-17, 1983, field test investigations was to study firsthand, and in detail, the pump startup problems at Fountain Valley. Numerous tests were performed involving several different plant configurations. The various test configurations were devised to investigate and possibly isolate the source of the startup problems; however, none of them were successful in doing so. However, from the test results, it became apparent that any effort to reduce the transient startup current improved the success rate of bringing units on line. Wiring the feedback transformer directly to the 2400-V bus reduced the inrush current by about 15 percent. This is equivalent to reducing the peak inrush current by 2.5 per unit on the 2400-V feedback transformer base. This resulted in the best, if not only, improvement in the success rate of bringing units on line. Prior to preenergizing the feedback transformer and filter, 8 of 12 startup attempts were successful in bringing a second unit on line. After wiring the feedback transformer directly to the 2400-V bus, there were no failures in bringing a second unit on line; and

7 of 10 startup attempts were successful in bringing a third unit on line.

Removing the capacitor from the circuit also did not improve the startup success rate. This does not mean that the tuned circuit does not contribute to the problem, but does show that the drive system problem is current disturbance related regardless of whether it is current inrush related and/or filter current oscillation related. This statement is based on the fact that most of the prior test data tend to strongly indicate that the current inrush surges initiated filter circuit oscillations, which aggravated the problem and contributed substantially to the 600-V bus (540-V transformer winding) waveform distortions.

During one test, accurate digital voltmeter bus voltage drop measurements were made to determine the motor terminal voltage drop of a loaded motor. The results showed that a loaded motor drops the 2400-V bus voltage from 1.2 to 1.5 percent per motor. This agrees with data and calculations from the June 6 and 7 tests, and further supports our contention that the power system is of sufficient capacity.

Direct currents of about 10 A were found to exist circulating between the converter and feedback transformer of each unit. This was probably due to the unconventional asymmetrical design of the converter. This current can only add to the startup problems by further increasing the percent distortion factor that the drive must contend with. Details of the testing, data, and analysis are presented in appendix G.

It appears that any modification made to reduce the initial current surge during startup would also reduce the severity of the 600-V bus voltage waveform distortions and thereby improve the drive system performance. The harmonic like current in the feedback transformer circuit of a running drive, either inrush or filter oscillation related, induces a voltage across the feedback transformer that results in a distorted 540-V bus voltage. The distorted waveforms produce errors in the converter gate firing circuits and/or failure of the silicon-controlled rectifiers to commutate properly, thereby resulting in phase-to-phase converter faults that trip the equipment off-line. The ability of the equipment to ride through this distortion is dependent on the percent distortion factor of the 600-V bus voltage (not the 2400-V bus). Furthermore, the percent distortion factor is a plant design parameter that must be considered in the selection of all associated equipment. In summary, during the initial stages of plant and equipment design, some thought should have been given to equipment integration and the resultant percent distortion factor to ensure proper plant operation.

FIELD EVALUATION OF PROPOSED MODIFICATIONS

The purpose of the December 6, 1983, test investigation at Fountain Valley was to evaluate the effectiveness of capacitor modification at the pumping plant in eliminating the startup problems and, if this was not a successful solution, to determine if reconnecting the feedback transformers to the line side of the unit contactors in conjunction with capacitor modifications would perhaps alleviate the starting problems. The final and most promising alternative was to investigate insertion resistor starting.

The capacitor modifications separately, and in conjunction with rewiring the feedback transformers to the line side of the unit contactors, were not successful in eliminating the startup problem. However, the insertion resistor scheme was most successful in eliminating the problem. There were 22 successful startups out of 22 attempts. Based on this success record, it was recommended that each of the Fountain Valley units be modified for insertion resistor starting.

The test records and analysis indicated the startup problem revolved around the high impedance feedback transformer that severely restricts the drive SCR to the point that the drives will not operate properly. The insertion resistor scheme eliminates the problem by reducing the SCR requirements of the drive. For a detailed analysis of the evaluation of the proposed modifications, please refer to appendix H.

APPENDIX A

PRINCIPLES OF SLIP LOSS AND ENERGY RECOVERY SYSTEMS

PRINCIPLES OF SLIP LOSS & ENERGY RECOVERY SYSTEMS

The wound rotor induction motor has proven through years of operation to be an extremely reliable, low maintenance, piece of equipment. Although it employs slip rings and graphite brushes, the wear and maintenance requirements are totally different than the requirements for a DC motor. With the exception of extremely severe atmosphere applications the slip rings are generally of a bronze composition and experience very little wear in operation. This eliminates any need for machining as is required with a DC commutator. Maintenance consists of periodic brush replacement every four to five years.

In operation the torque of the motor is related to the current that flows through the rotor windings. At maximum speed the rotor current is at its maximu. For a centrifugal load the rotor current reduces as the square of the speed, ie. 100 amp current at 100% speed = .8 x .8 x 100 amp or 64 amp at 80% speed.

Traditionally external resistance has been connected to the rotor slip rings as depicted in Fig. 1 to regulate the rotor current and hence motor speed. In viewing Fig. 1 it becomes readily apparent that the power flowing through the external resistance is dissipated as heat to the atmosphere.

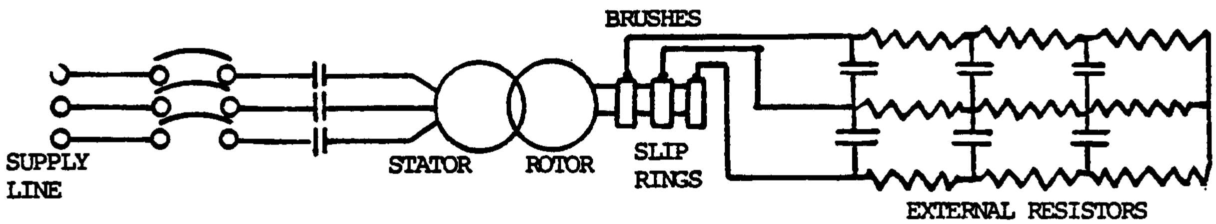

Fig. 1 - RESISTANCE CONTROL

Compare Fig. 1 The Resistance Control or Slip Loss System with Fig. 2 ECONODRIVE Slip Recovery System and the difference becomes readily apparent.

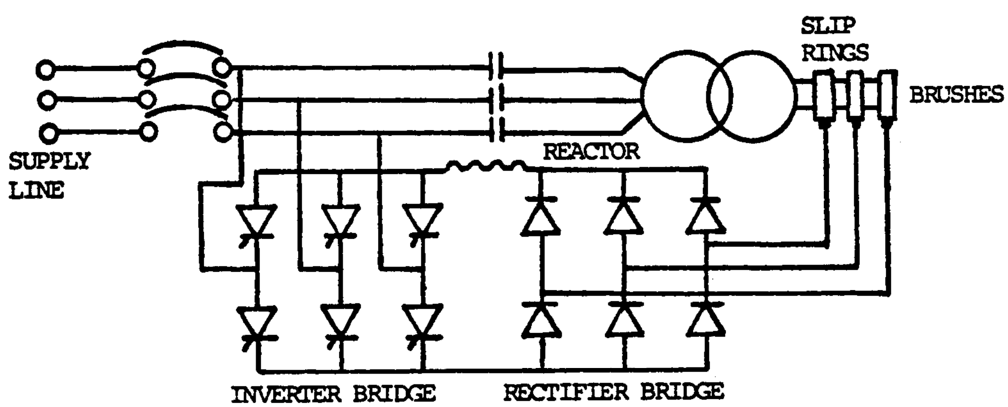

Fig. 2 - ECONODRIVE Slip Recovery System

In place of connecting the rotor to energy wasting resistors, the rotor power is taken through a rectifier bridge which converts the AC rotor current to DC current. This DC current is then directed through a reactor to an inverter bridge which converts the DC current to AC current in phase with the incoming supply line to which it is returned. This action recovers approximately 97% of the normally wasted rotor power.

PRESENTED TO TECHNICAL ASSOCIATION OF THE PULP AND PAPER INDUSTRY (TAPPI) 1980 ENGINEERING CONFERENCE

ADJUSTABLE SPEED OPERATION OF A-C MOTORS USING SLIP ENERGY CONTROL

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ABSTRACT

Experience with many successful slip energy a-c motor controls has demonstrated high reliability and proven the prospect of significant energy cost savings. Improved specialized techniques and increased understanding has extended the use of this equipment to applications exceeding 1000 horsepower. This paper attempts to develop a perspective of the state of the art and to identify the extent to which slip energy control fits into the general area of adjustable speed a-c drives.

INTRODUCTION

In the past several years in North America, hundreds of successful slip energy recovery drive installations have been established by a number of manufacturers. Until now most of these have been in the water and sewage pumping field with some excursions into general purpose fan and boiler induced draft applications. Originally, this equipment gained popularity as a replacement for less efficient adjustable speed apparatus, such as eddy current and hydraulic couplings and wound rotor motors operating with dissipative rotor resistance or reactor controls.

At first, the slip energy recovery drives were significantly more expensive than the dissipative controls they replaced, i.e. clutches and resistors.[1] Cost trade-off studies were developed and were ultimately justified to show that the incremental cost associated with applying a slip energy recovery unit could be returned in periods as short as three to five years. This principle has been sharpened in the face of continued increases in energy costs accompanied by a reduction in the relative cost of slip energy systems.[4 5]

The wound rotor motor seems destined to retain the advantageous qualities of its rival, the d-c drive and the variable frequency inverter, while rejecting or circumventing their disadvantages. That is, the wound rotor drive has the simplicity and reliability of the d-c drive, while the slip rings perform with carefree endurance rivaling the squirrel cage motor. Slip rings are used where commutators could or would not be tolerated.

A further and perhaps unwritten qualification has been reliability. Faced with municipal water and sewage pumping applications, the slip energy recovery drive is placed in situations involving unattended stations supervised by personnel completely lacking the electronic service and maintenance skills normally found in industries where thyristor equipment has been applied. With the exception of perhaps one or two of the very earliest vintage of equipment, the drives originally placed in the field are still operating today. The retro-fitting of some field modifications was required to follow established learning curves in a few specific instances. But the overall aspect of the slip energy drive continues to indicate efficiency, simplicity, reliability and serviceability equal to or exceeding any other form of electronic or electromechanical adjustable speed arrangement.

This paper will attempt to identify the state of the art in slip energy controls, explaining the structure and performance of existing equipment. The extension of the basic drive to higher powers of serveral thousand horsepower will be emphasized. The prospects of extending the application of these units away from their now traditional fan and pump applications to more general service is considered.

FUNDAMENTALS OF OPERATION

Figure 1 depicts a basic slip energy control arrangement. The system, motor and control, is fed by an input circuit breaker operating off the line voltage V_1. Once the breaker is closed, the control and a portion of the power factor correction capacitor (PFA) is permanently connected to the line. The motor stator and another portion of the capacitor (PFB) is energized through a contactor M during periods of running. Rotor or air gap voltage appears on the slip rings as a quantity transformed from the stator voltage and is given approximately by the expression:

$$V_r = (1 - x)\, V_{rm} = S\, V_{rm}$$

where V_r = rotor voltage

V_{rm} = maximum rotor voltage

x = relative speed, i.e. ($0 \leq x \leq 1$)

s = slip

The motor slip diminishes as the speed increases according to the expression:

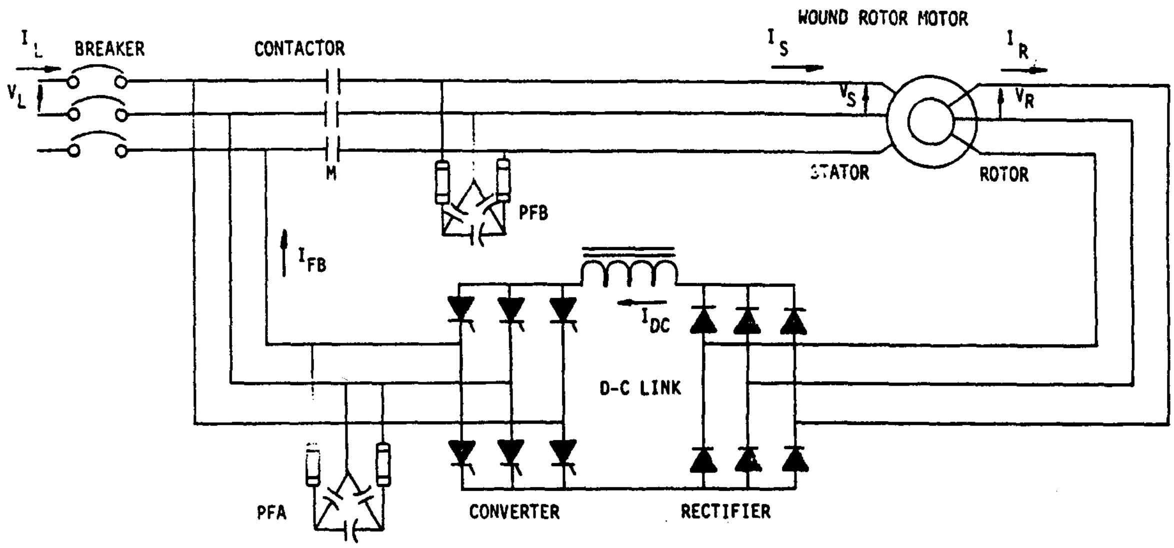

FIGURE 1- BASIC SLIP ENERGY CONTROL SYSTEM

$$S = \frac{W_s - W_r}{W_s}$$

where W_s is the synchronous rotational speed of the stator field, i.e. $W_s = \frac{120F}{P}$ (in RPM).

W_r = the angular speed of the rotor (RPM)

F = the line frequency; and

P = the number of rotor poles.

The frequency F_r of the rotor voltage is given by F_r = SF or (1 - x)F.

These relationships are plotted in Figure 2. In order to achieve speed control, torque which is proportional to rotor current I_r, must be developed over the entire speed range. This is done by rectifying the rotor voltage and applying the resulting normalized d-c voltage to a d-c link which in turn is applied to the d-c terminals of a phase controlled converter.

The converter, operating as a synchronous inverter transforms the link voltage back to the line. The d-c link is provided with a smoothing reactor to filter prospective harmonics developed as a consequence of rectification and phase control. The speed of the motor is controlled by adjusting the developed torque (I_r) as a function of load conditions. The rotor current, in turn is a function of the phase of the firing instants in the thyristor converter.

The overall scheme described is referred to as a slip energy control because the speed of the stator field is constant unlike the principle employed with an adjustable frequency inverter.[6] Faced with a constant speed stator field, the rotor responds like the rotating member of a friction clutch, except the

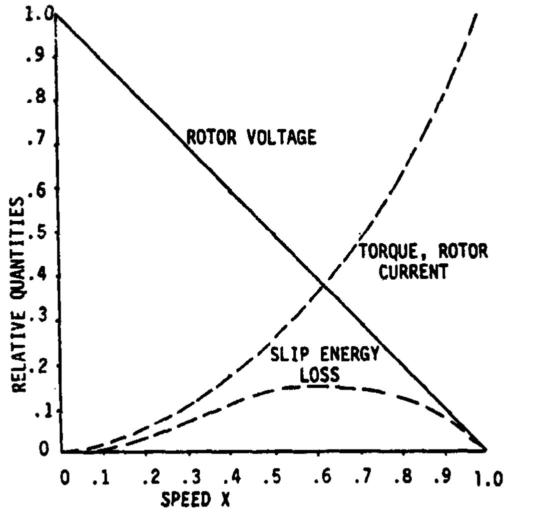

FIGURE 2 - ROTOR QUANTITIES AS A FUNCTION OF SPEED

coupling means is magnetic rather than mechanical. Nevertheless, the principles are the same. If T is the transmitted torque, then the following relationships hold:

Output power - KW_rT

Input power = KW_sT

$$\text{Efficiency} = \frac{KW_rT}{KW_sT} = \frac{W_r}{W_s}$$

K = constant

T = relative torque

Thus, for a basic wound rotor motor or a fluid or eddy current coupling operating with adjustable speed, the efficiency is proportional to speed.

Obviously, if the load characteristics were of a constant toroue nature, the loss of power at low speeds would be prohibitive. However, if a pump or fan drive is considered, where the load torque varies essentially as the square of the speed, the resulting power loss in some instances has been tolerable. Taking the specific case of a wound rotor motor operating from resistors, the slip energy lost is given by:

$$\text{Power in} = KW_sT = KW_sX^2$$

$$\text{Power out} = KW_rT = (X\ W_s).\ K\ X^2$$

$$= KW_sX^3$$

Power lost = power in - power out

$$\text{Slip energy} = K\ W_s\ X^2 - K\ W_s\ X^3$$

$$K\ W_s\ (X^2 - X^3)$$

$$K\ W_s\ X^2\ (1 - X)$$

on a relative or per unit basis the slip energy normalized to rotor input is given by:

$$P\ \text{slip} = \frac{\text{Slip Energy}}{\text{Rotor Input}} = \frac{K\ W_s\ (X^2 - X^3)}{K\ W_s} = X^2 - X^3$$

It can be shown that the slip energy under this condition (i.e. for a fan or pump) has a maximum value of 14.8% of the rated rotor input at 2/3 speed as plotted in Figure 2. In most applications, a fan or pump operates for sustained periods at or near 70% speed. Here, the proportion of power lost relative to the power transmitted is very high, in the order of 50%. With escalating energy costs and at higher power it becomes necessary to use systems which can recover or reclaim the lost slip energy.

Evaluation based on the added acquisition cost of slip energy recovery systems and the cost of borrowing money versus the incremental cost of energy have justified slip energy recovery in many instances. Even in cases of very low horsepower installations, the peripheral factors, such as cost of removing waste heat, etc. have justified the use of slip energy recovery where energy savings were not normally prescribed.

In very high power installations of over 1000 HP, obviously the added problem of disposal of waste energy in eddy current or fluid couplings and in some cases the increased size and cost of the physical structure associated with drive element (i.e. fan, pump) are of prime importance and have tipped the scale to slip energy recovery systems.

DRIVE DESIGN

As in the case of most industrial electronic drives, it is the aim to supply a system of integrated and co-ordinated components to perform a specific task completely. Often these are supplied in a single cabinet or a close knit array of enclosures. The ultimate goal is to accept the 3 phase power supply leads and provide the necessary motor power leads. Adding a relatively few interface connections to enable the motor, the control and the outside world to communicate with each other, the customer expects to find a working system complete with provisions for internal and external protection including the implications that the equipment will meet established standards and codes pertinent to the industry and/or environment which it serves.

SIZING & CO-ORDINATION OF COMPONENTS

The exact design of any power electronic system is a combination of emperical and analytical compromises. Analytical approaches are well defined with respect to establishing bottom line requirements of semiconductors, sizing of motor overloads and thermal protection, etc. For fan and pump loads these calculations are simplified because the duty cycles are not severe as in the case of some steel mill work profiles. Many other considerations such as breaker frame sizes, conductor sizes, are dictated by national and local codes and practices. Building from these, the designer can apply his own judgement with respect to safety factors above and beyond minimum requirements. Certainly, the level to which external and internal transients are suppressed as well as the margin between the absolute peak operating voltage and the peak blocking capabilities of the semiconductors are left to the discretion of the designer. Operating a synchronous converter almost exclusively in the re-generative mode involves particular precautions which are established in the art.[3] The particular method of suppression involves the co-ordination of suppressor networks, line or branch reactors and/or the semiconductors involved. These are beyond the scope of this paper, but it should be mentioned that the presence of power factor correction capacitors require special consideration, but also allows some advantages.

Fault protection is normally provided by fast current limiting fuses. It is possible, through oversizing of semiconductors, to eliminate fuses, allowing the breaker, in combination with electronic trip and safeguards, to protect against component failure and system malfunction. It would appear that from a reliability stand point, the most important parameter in selecting a semiconductor is fault current rating. While no particular blend of actions or recipes is suggested, it is obvious that a consistent, educated approach based on theory and experience produces extremely insensitive and durable equipment.

COOLING

It is believed that in most slip energy systems considered, equipment cooling should be accomplished via forced or convected air. The high efficiency of this equipment promotes such an approach. However, it is recognized that by not using liquid cooling of semiconductors, the rating of the largest available sub-system or module is restricted to 1000 to 2000 horsepower. This becomes a disadvantage only in a small portion of the present market. On the other hand, the use of multiple units present some advantages as will be discussed later. In a few installations the need to treat ambient air has led to some cabinet heating problems leading to a need to duct or direct air with more discipline. It is felt that the most effective way to deal with adverse ambients is to pretreat cooling air or to isolate the equipment in reasonably clean control rooms. Generally speaking, cooling air has not presented major problems even though the amount of air required for a large drive is significant. It has been found that the air supply does not have to be refrigerated or conditioned, but merely replenished to guard against self generated heat build-up.

ANALYSIS OF DRIVE OPERATING PARAMETERS

Frequent questions arise concerning the distribution and characteristics of the current within the slip energy control. Beyond line current draw, it is important to know the nature of the drive current, i.e. I_{fb} in Figure 1. This is necessary not only to understand the inner workings of the drive, to be able to size the power factor correction capacitors, but in some instances to be able to feed recovered energy back to a separate source of power.

MOTOR STATOR CURRENT

Figure 3 depicts a typical motor stator current as reflected by a motor operating into a dissipative control means, i.e. a resistor controlled wound rotor motor. Since the rotor energy is lost in heat, the reflected line load is high and the values can be used as a basis of comparison in later discussions of the slip energy recovery system. For fans and pumps at low speeds the load diminishes to essentially zero, the stator current reduces to the magnetizing current of the motor. This situation is discussed in Appendix "A" using an approximate equivalent circuit.

ELECTRONIC CONTROL CURRENT-FEEDBACK CURRENT

In analysing the current returning from the rotor, after processing by the control, some assumptions should be made:

1. The d-c link inductor is sufficiently large to smooth the rectified rotor voltage, so that the current ripple harmonics are not significant.
2. The stator sub-harmonics will be ignored since they are small and secondary in nature with respect to power flow.
3. The line source impedance of the phase controlled converter is small.
4. For the purpose of fundamental power flow, the reflected converter current harmonics are neglected. This is a valid assumption for power flow because of the filtering action of the power factor capacitors.

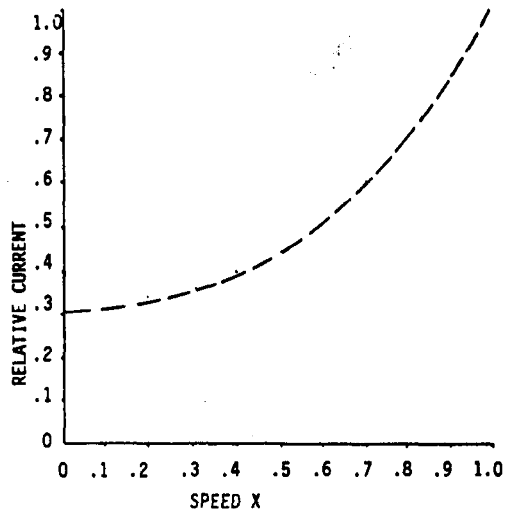

FIGURE 3 - MOTOR STATOR CURRENT vs. SPEED

For an optimized drive, the rotor voltage at stand still is 0.9 times the stator applied power supply voltage. This is in keeping with providing an adequate commutation margin for the converter. Thus, when power is first applied, the firing instants of the converter are assumed to be at their maximum retarded position. The converter assumes a maximum inverter voltage which exceeds the rectified no load rotor voltage. As the firing instants are advanced in response to a speed regulator error calling for torque, i.e. rotor current, current begins to flow as the converter link voltage equals and drops slightly below the rectified rotor voltage.

$$V_c = \frac{3}{\pi} V_{1p}(\cos \theta_1)$$

where V_c = average converter d-c volts

V_{1p} = peak line voltage

θ_1 = converter phase firing angle ($90^\circ \le \theta_1 \le 180^\circ$ for inverter operation)

The maximum value of θ_1 is a function of the maximum induced rotor voltage, V_{rm} developed at motor stand still according to the motor transformation ratio "a". Again, this is normally chosen so that this transformation ratio, stator to rotor, is no greater than 0.9:

(i.e. $V_{rm} = a\ V_1 = .9\ V_1$).

thus θ_1 max = $\cos^{-1}(a)$

similarly θ min = $\cos^{-1}(0) = 90^\circ$ (full speed)

for this typical set of parameters: $\cos \theta_1 = a(1 - x)$.

The fundamental current returning to the line at the converter supply terminals has very nearly the same phase as θ_1. If it is assumed that the load follows an ideal fan or pump square law characteristic, then:

$$I_{fb} = I_r \angle\theta_1$$
$$= X^2 I_{rm} \angle\theta_1$$

where I_r is the fundamental or sine component of rotor current.

I_{rm} is the rated rotor current.

X - speed

and; I_{fb} is the converter a-c current.

These relationships are plotted in Fiqure 4. Note that at maximum speed the rotor current is maximum and in quadrature with the line voltage. This would produce a prospective adverse power factor if not corrected with a power factor capacitor current I_C. The amount of correction required is dependent on the peak converter current generated. In most cases, this value is equal to or slightly greater than rated load current. For a single range drive, one may consider the amount of P.F. current required as 1.0.P.U. or equal to the rated stator current. Such a correction will counter the effects of the lagging power factor feedback current and restore the system power factor to essentially that of the motor operating at full speed and full load as a squirrel cage motor, i.e. with shorted slip rings. For medium size motors, the full load power factors range from 0.8 to 0.9, essentially the same as squirrel cage motors.

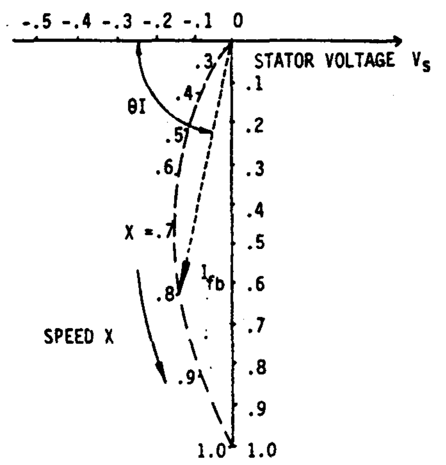

FIGURE 4 - FEEDBACK CURRENT I_{fb} VS. SPEED

LINE CURRENT DRAW TOTAL SYSTEM CURRENT

If the feedback current and the stator current are summed with the total power factor correction current, a total system current can be derived. The results are plotted in Figure 5. It should be noted that over the normal operating speed range, the current approaches a value proportional to the cube of the speed. This indicates that the drive is not only efficient by that the line current is drawn at a power factor approaching unity.

The power factor assumes the motor P.F. at full speed and changes from lagging to leading at approximately 70% speed. It should be noted that the power factor departs from unity only at relatively low and perhaps insignificant line currents. This is partly due to the low losses in the system relative to dissipative controls, i.e. the lower the losses the worse the power factor. While the line current rises at low speeds due to the excess or unused power factor correction current, operation in this range is normally not required. However, if power factor at low speed is critical the capacitors can be shed with contactors to bring line current draw to an absolute minimum. It should be recognized that the power factor capacitors serve an added function of inhibiting line transients and protecting the drive thyristors.

QUALITY OF LINE CURRENT DRAW

In addition to reflecting a low line current draw coincidental with high power factors, the slip energy control demonstrates a further advantage. Starting a motor with slip energy control does not involve a surge or inrush of current. The contactor closes on an open circuited rotor, current flows only after a delay and only under controlled current

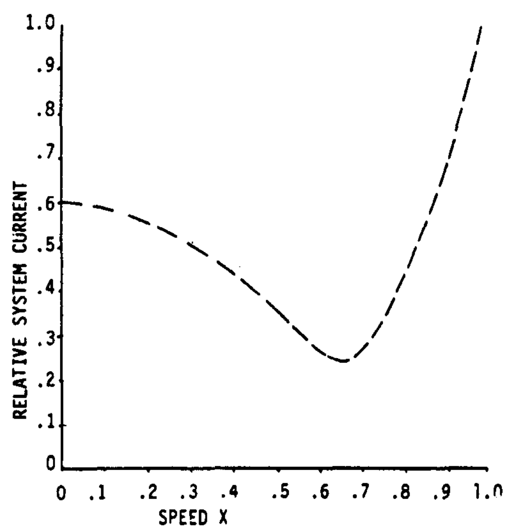

FIGURE 5 - TOTAL SYSTEM CURRENT VS. SPEED

limit. This not only has the advantage of reducing wear on the contactor, but is extremely valuable when sizing diesel driven alternators for emergency run operation. This, of course, is a frequent consideration in sewage and water pumping applications where the service is absolutely vital in spite of power failures.

It has been mentioned that like any phase controlled converter system, specific line current harmonics are generated. In most industrial installations, i.e. steel mills, these are tolerated while in some sensitive areas, i.e. laboratories, line filters are employed to eliminate electro-magnetic interference. The use of power factor capacitors in slip energy control generally contains the harmonics and interference to levels below those generally experienced with phase control equipment.

SYSTEM LOSS

The principal losses of a slip energy drive system are found in the motor. It has been mentioned that the wound rotor motor efficiency is comparable to the squirrel cage motor namely 91% to 95% depending on the size and particular design. The slip energy control introduces an additional 1% to 2% loss depending on the configuration. The losses are normally distributed primarily between the d-c reactor and the semiconductors with measureable but small contributions from the capacitors, the control coils and power supplies, fans and suppressors. In short, adding a slip energy control to a motor introduces less losses than inserting an isolation transformer.

BASIC CONTROL FUNCTIONS

Figure 6 depicts the overall control strategy. The scheme involves a nesting of three control loops, i.e. current, voltage/speed and process regulators. Such an arrangement is common to most thyristor phase control equipment. The primary or inner most loop is the current regulator. Rotor current is sensed indirectly, at the line voltage terminals of the converter which is one of the three possible points of measurement, i.e. including the rotor current and the d-c link current. The advantage of sensing at the converter terminals is that the frequency is high, reducing sensor filtering requirements and allowing the use of conventional current transformers. No noticeable disadvantage of this method is apparent. While direct rotor current measurement is possible, it is not recommended because of the special CT's required at high speed at frequencies approaching 1 hertz.

The parameters of the current loop are such to achieve cross over at approximately 400 radians providing a fast closed loop current response. The principle time factor associated with the current loop is the inductance and resistance of the d-c link reactor. The current regulator operates in a linear mode until it reaches a maximum current limit value as determined by an adjustable current limit setting, typically 10% above the rated rotor current. An over current trip is provided in the event of an excessive excursion from the maximum current limit setting.

As an alternative to using a tachometer, the rotor voltage can be used as a means to synthesize

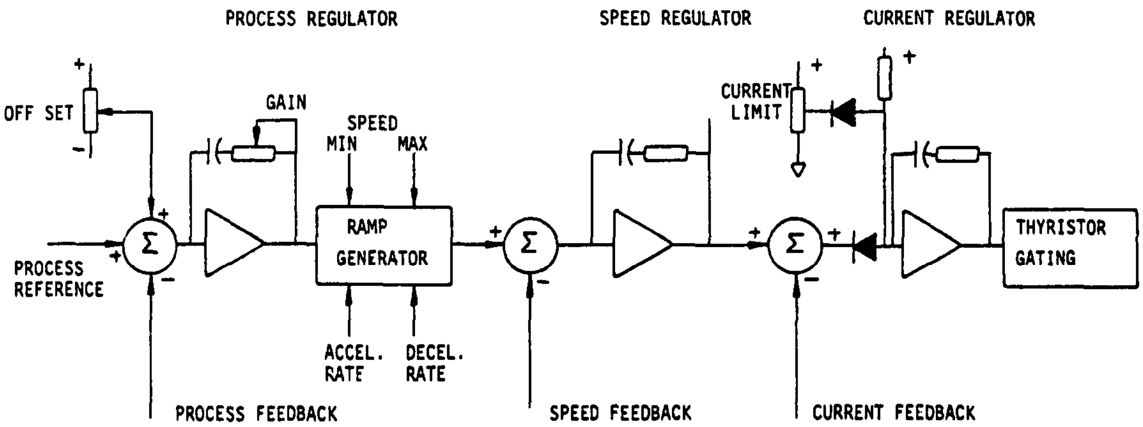

FIGURE 6- CONTROL LOOPS - SLIP ENERGY CONTROL

a speed signal. The rotor voltage can be sensed by using a pair of transformers whose output when rectified and filtered provides a d-c quantity proportional to rotor voltage. Since the rotor voltage is maximum at zero speed and diminishes to zero at synchronous speed, a bias signal is used to null or offset the rotor voltage at zero speed. This combination of zero offset bias and rotor voltage produces a remarkably accurate representation of speed. It is this signal which can be used to direct the speed loop, produce a speed read out and in some cases provide information required to supervise the drive, i.e. sequence contactors and verify operating status, etc. In rare cases, where extreme accuracy of speed setting and/or speed indication is required, a tachometer must be provided. For small drives the use of a tachometer should probably be discouraged because of difficulty in mounting and cost. Generally speaking, the process using the motor drive involves speed only as an indirect quantity, with a primary controlled quantity being pressure, flow, temperature or level, etc. In these cases a speed signal derived from rotor voltage is more than adequate in that it merely serves to stabilize the speed control loop. Obviously, rotor voltage sensing is similar to armature voltage sensing in d-c drives.

In order to provide a means of acceleration and deceleration control, a ramp generator should be provided to prevent mechanical or system transients. Typical values of standard ramp time adjustment are 2-20 seconds, while in some instances ranges to 200 seconds have been specified. In some applications a maximum speed limit is necessary. This can be achieved directly using a max/speed setting or indirectly by limiting the rotor current. A minimum speed setting is almost universally required with fan and pump loads. In small fans no significant work is accomplished below 30% speed. In larger fans and pumps minimum speeds are rarely set below 60% of maximum. A minimum speed is set by a bias in the process regulator or a clamp in the reference generator.

To add flexibility, a process regulator can be provided as a front end to a slip energy control system. Often the responsibility for control and stabilization of the process is accomplished outside the drive system, the only input being typically a 4-20 mA or 3-15 PSI, etc., speed reference. The process regulator, if provided with a gain or bias control, can still be useful in providing a scaling of the reference input and a means of providing process offsets, i.e. minimum speed setting offsets for minimum quiescent inputs, etc.

A variety of hardware exists to convert pressure and current references to standard regulator quantities, i.e. 0-10 V d-c. As in any industrial situation any control input must be guarded or buffered against abuse. A prime enemy of reliable controls in fan or pumping applications is lack of integrity of connection in the presence of moisture and corrosive atmospheres. This has the tendency to discourage the use of plug-in cards and favors the more robust screw type connections.

INTERNAL PROTECTION

Most phase control electronic drives are equipped with an almost standard set of internal protective measures. These include protection for:

-Incorrect Phase Sequence
-Single Phasing
-High/Low Line Voltage
-Control Power Supply Loss
-Current Limit/Overcurrent Trip
-Internal Component Overtemperature (heatsinks, etc.)-
and perhaps serveral specialized functions pertinent to a particular control.

These features are provided to guard against improper supply conditions as well as failure of some portions of the drive itself. It is recognized that these features are or should be part of any electronic control.

TYPICAL START-UP SEQUENCE

When power is first applied to the control, a time delay is initiated to inhibit all contactors and all thyristor gating amplifiers. All regulators, integrators, ramps, etc. are clamped to zero. The phase control circuits are set to maximum phase retard. During this delay period a logic scan is made of the vital signs and status of the control. The activation of any protective element, high/low line, control power supply loss, etc., will inhibit further action and annunciate the condition. Continued successful logic scans will, after the initial delay, allow the activation of the motor contactor. A run command will now initiate a second short time delay to confirm system status after which gates and regulators are released. The control will function until it is told to stop or until a continued logic scan reveals a system flaw. At this time the system will attempt to execute an orderly logic shut-down unless faced with a system fault due to internal failure, i.e. motor short, thyristor failure, etc.

The above sequence is slightly more complicated in dual range or multiple unit high power systems. Generally speaking, the amount of interlocking is small and uncomplicated.

MOTORS

Once the shaft horsepower is determined, the selection of a wound rotor motor is primarily a function of the motor manufacturer's practice. Beyond specifying the fact that an electronic slip energy control system is to be used and a request for a particular rotor voltage, no special considerations are required on behalf of the control manufacturer. Certainly routine specifications such as type of winding and bearing temperature protection, type of bearings, winding treatment and insulation type, special finishes, colour, etc., are defined. It is assumed that no conscious oversizing or special design is implied in designating a motor for slip energy recovery. Oversizing is expected for constant torque and severe duty. It is possible to specify a Service Factor or a Class "B" temperature rise with Class "F" insulation, etc., for safety. However, slip energy units have had popularity and extreme success in retro-fit applications using existing wound rotor motors. Thus, while the presence of harmonics in the slip energy system is recognized, the effect with respect to motor consideration appears to be negligible.

With regards to motor protection, it is felt that the principal and most effective means of guarding against motor damage in any adjustable speed application is in the winding temperature sensor. Other forms of protection such as thermal overload relays are not effective due to the inherent ability of the control to limit the current to 100% of rating. On the other hand, a motor faced with a locked rotor could experience a tendency for overtemperature due to loss of fan cooling. In such instances, the motor temperature protection (thermistors, RTD's, etc.) would automatically sense this condition and open the contactor. Furthermore, the type of protection is not as critical as in fixed speed applications since the temperature rise is usually under current limit, producing a slower rate of temperature rise. The need to anticipate a hot spot is thereby lessened.

EXTENDING THE OPERATING RANGE OF SLIP ENERGY RECOVERY SYSTEMS

As previously stated, under normal circumstances the maximum voltage which can be applied to the rotor rectifier terminals of a slip energy recovery system is slightly lower than the supply voltage. Converter systems are normally optimized for a maximum standard supply voltage, i.e. 480 volts, 600 volts, etc. It is normally not practical to extend this range, hence the maximum applied rotor voltage for a slip recovery drive is limited. Frequently it is inconvenient to obtain motors at a particular voltage forcing the designer to accept a rotor voltage, either higher or lower, than the optimum for his control. At times, it is possible to use a full time rotor matching auto-transformer. However, this is costly and has other undesirable effects. Opting for the lower voltage reflects a higher current for a given horsepower which in turn requires an oversized control since the supply voltage is constant. A higher than optimum voltage which reflects a lower current and reduces the control rating can be accommodated under certain circumstances. Since the rotor voltage diminishes with speed, a starting resistor can be used to accelerate the motor to a speed at which the operating rotor voltage is below or within the maximum capability of the control (Figure 7). At this point, as monitored by a speed or voltage sensor, the electronic drive is activated, controlling the speed in a continuous manner above this minimum critical speed. Restricting motor operation at low speeds is not normally important since, as mentioned before, high power fans and pumps are normally operated only above 50% speed.

In some instances the available rotor voltage is only slightly higher than the control capability. This requires acceleration to only a low speed. The starting resistors are thus required to "kick-up" the motor to operating speed. In other cases the available voltage may be greater than 150% of the controls capability which may require that the rotor be isolated from the control by a disconnecting contactor. The motor would have to be accelerated to a higher minimum speed of 30% to 40% of the rated speed before the contactor could close and the control could be activated.

The cost of the contactors and resistors is more than offset by the reduction in control size resulting from the greatly reduced reflected current. Such schemes for extending the capabilities of a control are extremely effective at high horsepowers.

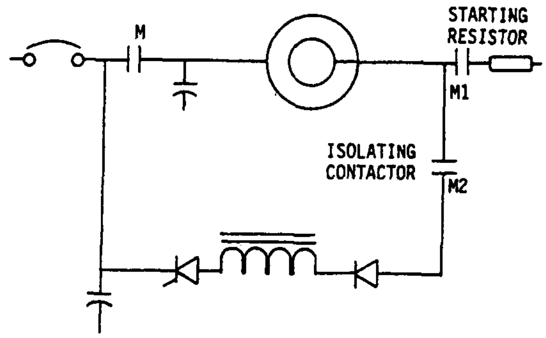

FIGURE 7 - SLIP ENERGY RECOVERY SYSTEM USING STARBTING RESISTORS.

DUAL RANGE DRIVE

If, because of high required available rotor voltage, it is still necessary to operate over a wide speed range, a dual range drive as shown in Figure 8 can be employed. The dual range drive employs a rotor transformer to match the high rotor voltage to the drive capability at low speed. At starting and at low speeds, the transformer steps down the rotor voltage through contactor M1. At higher speeds when the rotor voltage is reduced, M1 is opened and the rotor is connected directly to the control through M2. The control then operates the motor over the high speed range until such time as low speed is desired and the contactors sequence downward, M2 opens and M1 closes, etc. If hysteresis or an overlap range is built into this arrangement, transitions up and down are relatively infrequent and present no problem in contactor wear. This is especially true since in using logic, the transitions can be made at zero rotor current. Experience has shown that sequencing between speed ranges is so smooth that the action is virtually imperceptible with respect to motor torque and speed disturbances.

The sizing of the rotor transformer and low speed contactor are a function of the point of transition and the particular load characteristic. Assuming a maximum relative speed point of transition of K ($0 \leq K \leq 1.0$) and a normal pump or fan curve $I_r \sim X^2$, then the size of the rotor transformer is given by:
Rotor Transformer KVA = K^2 (Motor Horsepower).

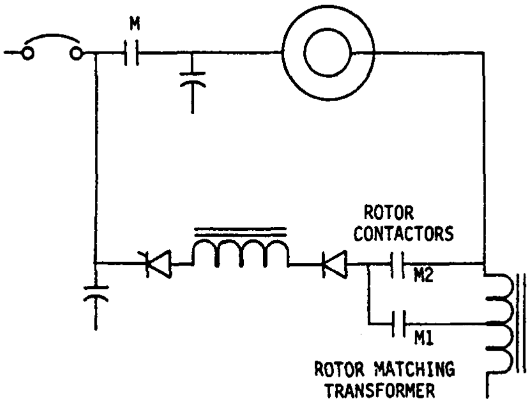

FIGURE 8 - THE DUAL RANGE CONTROL

Thus, a transition point of 60% of maximum speed or K = .6 on a 1000 HP drive would require a rotor transformer of approximately 360 KVA. Figure 9 depicts a typical dual range drive with respect to rotor, drive currents and voltages for the two distinct ranges of control. Here a transition to the high speed range is given as K1 = .6, the transition from the high to the low range if K2 = .5 which is also the rotor auto-transformer ratio N. Hence, observing a normal transformer relationship in the low speed range, the drive sees a voltage of N · V_r and a current of $\frac{I_r}{N}$ that is half the rotor voltage and twice the rotor current. In the high range the control faces the actual rotor volts and amps. With this particular set of circumstances it should be noted that because of the particular choice of rotor voltage and transition points, the control used is one half the motor size. A 500 HP control has the capability of controlling a 1000 HP motor over its entire speed range.

A dual range control has been employed in a boiler induced draft fan application where periods of sustained operation in the low speed range are required. It has been observed that under some conditions, due to changes in fuel and boiler conditions, transitions between speed ranges have been required. These are infrequent and present no problem with respect to system performance.

In most fan and pump applications where operation is restricted to the high speed range, the rotor transformer size is reduced to that of a starting unit. The low speed range is used only for smooth starting. In the high speed range, the combined effect of direct connection to the rotor and the use of high rotor voltage, i.e. low current, produce the highest efficiency. That is, since the current and energy handled is lower than for a full range drive, the losses are lower. Thus, the advantage of a dual or half range drive can be summarized as lower costs, smaller size and higher efficiency.

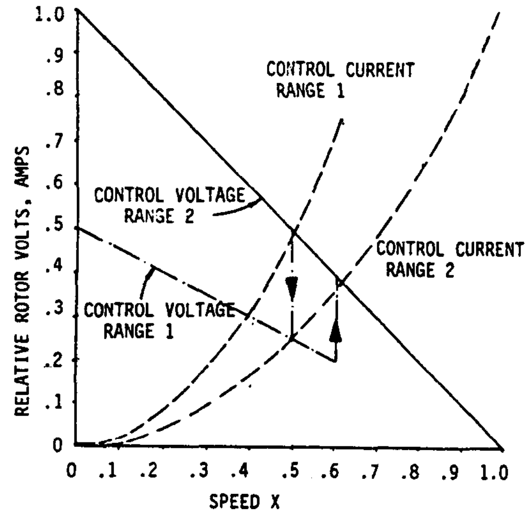

FIGURE 9 - ROTOR QUANTITIES AS SEEN BY A DUAL RANGE CONTROL

APPLICATIONS AT HIGHER HORSEPOWERS

When motor horsepowers exceed several hundred horsepower, a distinct advantage is seen in utilizing high supply voltages, i.e. 2300, 4160, 13 KV, etc. Certainly at 1000 HP the use of 460 volts becomes intolerable when the masses of cables required are considered. In such high power areas the use of slip energy controls present a number of distinct and strategic advantages over both the d-c drive and the variable frequency inverter.

1. Using a dual range slip energy control technique, a control sized at only one half of the total motor power needs to be used.

2. High voltage can be applied directly to the wound rotor motor stator, the machine acts as, and replaces a transformer applying a lower more manageable voltage to the control in the rotor circuit.

3. Phase control semiconductor devices can handle significantly more power for a given fusion diameter then those fast switch devices developed for forced commutated inverter operation.[2] This also means that the basic building blocks of slip energy control are adaptable to higher voltages.

4. The wound rotor motor has virtually all the essential values of a squirrel cage motor and at high power approaches its cost. The electronic control cost is normally less than the inverter.

Fiqure 10a deplays a typical high voltage/high power slip energy arrangement. It utilizes the principle of dual range control, but in addition incorporates a necessary feedback step-down transformer. The transformer is sized at only a fraction of the motor horsepower rating and **is the only significant difference between a high voltage and a low voltage control. All of the remaining hardware is of a low voltage nature.**

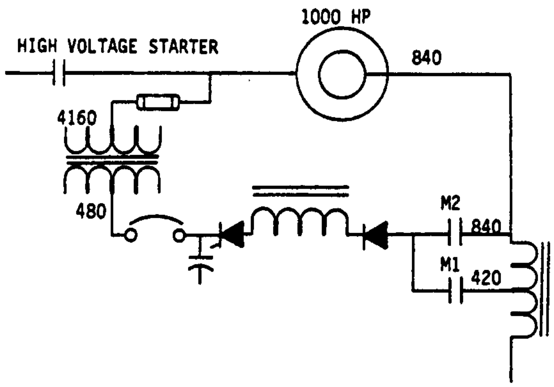

FIGURE 10a - TYPICAL HIGH VOLTAGE SPEED CONTROL

Higher power motors are accommodated by applying controls in parallel as shown in Figure 10b. In this case, a feedback transformer using dual secondaries is used. Using such an arrangement the controls become redundant in that one module may be disconnected for service with half the original power capability intact. This, for the case of a centrifugal load, implies the ability to operate at 70% motor speed with one unit out of service. Methods have been developed which allow both controls to operate independently, i.e.

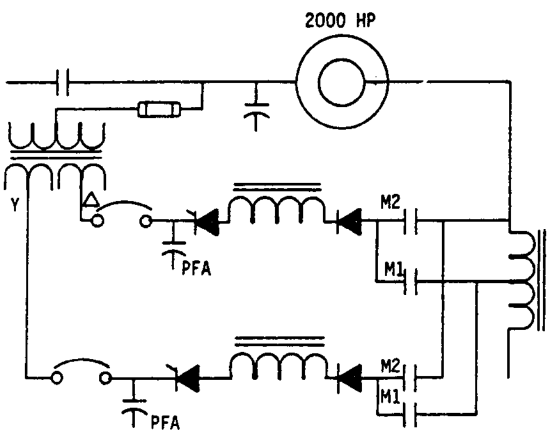

FIGURE 10b - HIGH VOLTAGE CONTROL WITH PARALLEL UNITS

each with their own control logic protection, etc. However, when wired together, even the logic functions can act in parallel. The loss of one regulator, for example, is replaced, on the fly, by the remaining functioning logic.

The technique can be extended to three or more modules, the degree of redundancy increasing with each addition. The prospects for zig-zagging to cancel the line harmonics generated by each converter are obvious.

LIMITATIONS OF SLIP ENERGY CONTROLS

While most aspects of the slip energy control seem applicable to most adjustable speed tasks there are several limitations.

1. Slip energy drives are capable of dynamically forcing torque in only the accelerating direction. Electric braking, although possible, is not as yet a normal function as in the dual converter d-c drive. The action is similar to a single converter d-c drive or perhaps a non-regenerating variable frequency drive. In this sense, a small fraction of adjustable speed applications such as fast position controllers are not candidates for the slip energy drive.

2. In some cases, hazardous areas will not permit the use of TEFC slip ring motors. Squirrel cage motors tend to be more suited to those applications. In many cases however, TEFC or open motors using purged slip rings are tolerable.

3. Extra high speed applications, i.e. above 3600 RPM could be a problem with wound rotor designs.

CONCLUSION

The slip energy control system has been applied in industry and is finding more applictions with time. There are few limitations in the control and motor combinations; brushes in particular have been no problem. In high power and/or high voltage supply situations the slip energy control appears to have inherent advantages over the d-c drive and the forced commutated inverter drive. Although built on old established principles, the technology has been brought up to date in the past several years with many benefits in cost and flexibility. Recent development indicates that the technology has further room for significant improvement and innovation.

APPENDIX "A"

DETERMINATION OF WOUND ROTOR MOTOR STATOR CURRENT WITH EXTERNAL RESISTANCE

Given the equivalent circuit of Figure 11, the motor stator current can be determined over the speed range:

where X_m is the magnetizing reactance.

V_1 is stator voltage.

a = the stator to rotor transformation ratio.

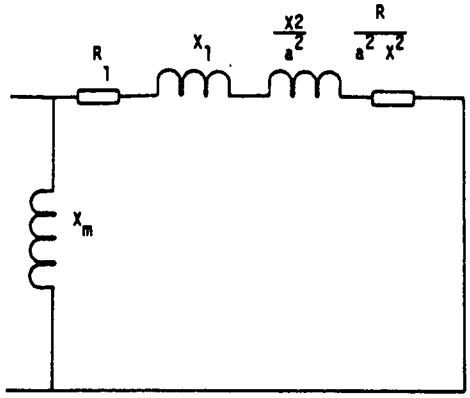

FIGURE 11 - MOTOR EQUIVALENT CIRCUIT

V_r, I_r = rotor parameters.

X_1, X_2, R_1 and R_2 are the normal equivalent circuit parameters.

V_{rm}, I_{rm} = rated values of rotor parameters

R_r^* is the rotor current referred to the stator.

R_r = base rotor resistance = $\frac{V_{rm}}{I_{rm}}$

Since, for a fan load operating at speed X:

$T \quad X^2$

V_r = rotor voltage - aV_L

$$R = \frac{V_{rm}}{I_{rm}} = \frac{aV_L}{I_{rm}}$$

The inserted rotor resistance.

$$R_r = \frac{V_r}{I_r} = \frac{(1 - x)\ V_{rm}}{X^2\ I_{rm}} = \frac{1 - x}{X^2} R$$

$$R_r^* = \frac{1}{a^2}\ \frac{R_r}{S} = \frac{R}{a^2\,X^2}$$

REFERENCES

1. A. LAVI & R.J. POLGE - "Induction Motor Speed Control with Static Inverter in the Rotor" - IEEE Transactions on Power Apparatus and Systems, Volume PAS-85, No. 1, January 1966.

2. W.E. NEWELL - "Dissipation in Solid State Devices" - IEEE Power Electronics Specialist Conference Record, pp 162-173, June 1974.

3. J.B. RICE - "Design of Snubber Circuits for Thyristor Converters" - IGA/IEEE Conference Record 1969, p 483.

4. D.L. DUFF - "Electrical Energy Conservation with Modern Variable Speed Controls, Canadian Pulp and Paper Association, 63rd Annual Meeting, February 2, 1977.

5. D.L. DUFF - "Energy Consumption and Maintenance Considerations for A-C Variable Speed Drive Systems, Canadian Council on Electrical Maintenance Inc., 21st Technical Conference, October 21, 1976.

6. B. MOKRYTZKI - "Pulse Width Modulated Inverters for A-C Motor Drives, IAS International Converter Conference Record 1966, Part 1, pp 8-23.

APPENDIX B

PRELIMINARY TEST REPORT BY MANUFACTURER

ROBERTS CONSTRUCTION COMPANY
P.O. Box 17248 T.A. Denver, CO 80217
4100 Garfield Street, Denver, CO 80216 • (303) 393-0666

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November 24, 1982

BUREAU OF RECLAMATION
P. O. Box 25007
Denver CO 80225

Attention: D-241
Mr. John Ogle

Re: Pumping Plants Nos. 1, 2, 3 and 4 - Fountain Valley Conduit
Fryingpan-Arkansas Project, Colorado - Spec. No. DC-7416

Subject: Pumping Plant #1 - Pump Units #1 - #4
Pumping Unit Electrical Failures

Gentlemen:

On September 27, 1982 pumping start-up operations began with pump units at Pumping Plant #1. Only pump unit #1 was available at this time.

Due to several factors only one pump unit at a time was attempted to be started until November 5, 1982, at which time Mr. Al Mumaugh, Anvic Corporation representative, attempting to start a second unit while the first unit was at operation speed had the first unit immediately trip out, indicating overload conditions. Although further attempts and tests were made by Mr. Mumaugh during the day he was unable to correct this problem and terminated the test for review at their plant.

On November 9, 1982 Mr. Mumaugh again arrived at Pumping Plant #1 to continue the testing and was able to conditionally operate two units at one time although he was again unable to resolve the problem and left to return to their plant on November 12, 1982. At that time directives were issued to all involved subcontractors to have equipment and personnel available on Tuesday, November 17, 1982 to resolve this problem.

On November 17th Mr. Don Faulkner, president of Anvic Corporation, arrived along with Mr. Mumaugh and a consultant, Mr. Hans Mueller. They were also accompanied by Mr. Kent Knight, a representative of Goulds Pumps and representatives of Cobb Plumbing and Heating. Howard Electric representatives and Square D representative, Joe Tucker, were also present.

Tests were run and the results reviewed through November 19th without being able to correct the problem in startup of the multiple units. Mr. Faulkner advised they felt they had isolated the problem and would issue a full report.

NOTICE
If you detach enclosures, please insert your code number______

We have now received the report from Anvic Corporation. This report identifies they have found their equipment to be correct and that the problem evolves within the 2400 Volt power supply system.

We are therefor transmitting this information for review by your design personnel and are requesting a meeting be established whereupon all available information can be discussed and a solution to the problem hopefully being made.

Your prompt review of this data, together with the Contract design data, will be sincerely appreciated.

Very truly yours,

ROBERTS CONSTRUCTION COMPANY

Richard O. Rarrick

Richard O. Rarrick
Project Manager

ROR/mt
cc: BofR, Salida
Cobb Plumbing and Heating
file

ANVIC INTERNATIONAL

1270 WINCHESTER PKWY., SUITE 200,
SMYRNA, GEORGIA 30080 (404) 435-3888

November 22, 1982.

E'VE MOVED
\NVIC INTERNATIONAL
SOO NEW McEVER ROAD
CWORTH, GA. 30101
EL: 404/974-7182

Mr. Frank Steele,
Cobb Plumbing & Heating,
2906 West Morrison,
Colorado Springs, Col.
80904

Subject: Fountain Valley Project

Dear Mr. Steele:

Attached please find our report on the tests carried out at the job site over the last two weeks.

We will not have our Consultants Report available for another week but per your request are forwarding the preliminary rêport.

We must point out that the conditions we have seen and operated under have provided electrical and mechanical stresses on the ECONODRIVE, notably the Contactors, Breakers, SCR's and those elements connected with the commutation circuitry. Shortened equipment life with premature failures may well be the results of these conditions. Consideration should be given to inspection of the Drives and refurbishment of those portions with obvious degradation carried out.

There is no way that such inspection will discover problems with SCR's, commutation circuitry, etc. and we will have to wait and see what problems, if any, occur.

Unless otherwise informed, we will plan to attend a meeting on December 3, 1982 at the Bureau offices in Denver.

Yours truly,

Don Faulkner.

Encl.
cc: Goulds Pumps
Denver, Col.

OFFICES IN: ATLANTA, GA. • PENSACOLA, FLA. • TORONTO, CANADA

PRELIMINARY TEST REPORT

November 22, 1982

Introduction

During attempts to run two ECONODRIVES on line simultaneously it was found that the first drive on-line would trip off instantly upon starting the second drive.

This trip-off was found to be independent of which two drives were involved.

Testing was then conducted over a two week period in order to determine the cause of this problem.

Procedure

The first week of testing was carried out with operational drives, i.e. under operating loads, in order to determine the mode of shut-down.

Consultants were hired by ANVIC and each test was analyzed at ANVIC's facility, and by our consultants prior to running the next series of tests.

The drive system was tested sub-system by sub-system by isolating its effect on the shut-down and thereafter eliminating that sub-system as the cause should the shutdown still be experienced.

The results of the first week's tests were analyzed and our Consultant, along with ANVIC developed a second series of tests which were carried out during week two.

These tests were carried out under the supervision of our Consultant, Mr. Hans Mueller, and were based upon the preliminary conclusions that the shut-downs were due to interaction between drive systems across the 2300V lines.

Observations

The mode of shutdown of the on-line drive was determined to be misfiring of the SCR's due to voltage distortions on the 600V lines inside the ECONODRIVE.

The misfiring resulted in effective bridge-shorting such that overcurrents and voltage collapse occurred within the on-line drive system. As the drive reaction time (in monitoring system out of specification conditions) is much faster than Starter, shut down would occur prior to the Starter dropping out.

These failures were extremely hard on the ECONODRIVE's as voltage and current overloads were being experienced with regularity.

The second series of tests were thus carried out with gating inhibited as by that time the problems were isolated to "upstream" of the commutation circuitry.

This second series of tests showed that the immediate static connection of the first motor and ECONODRIVE to the 2300V lines resulted in voltage dip on the 2300V lines and 600V line distortion, both of which would be ridden out by the Drive System.

Connection of the second ECONODRIVE to the 2300V lines caused a similar distortion which would be ridden out by the second system, but would cause failure of the on-line system if it were actively gating.

This voltage distortion was due to higher than expected current drawn from the first system feedback transformer thereby upsetting the voltage stability in that ECONODRIVE.

Conclusions

Shutdowns as experienced are not due to ECONODRIVE failures per se.

The drive system is misfiring due to distortion of the 600V lines which is in turn caused by cross talk between the two Drives over the interconnecting 2300V lines.

This cross talk, more commonly called Power Line Pollution is due to the inability of the 2300V lines to maintain stability when called upon to provide the inrush current required upon immediate static connection of a complete Drive System.

This lack of stability or stiffness is not due to excessive current requirements by the ECONODRIVE.

Unless the 2300V distribution system is stiffened, operation per specification, i.e. more than one system on-line with sequential starting separated by 25 minutes is impossible.

Supplementary Notes

In order to investigate the degree of 2300V line stiffness required, three additional tests were carriedout utilizing 2300V capacitors on the power bus.

With 500 KVAR connected to the power bus in the Starters, the voltage distortions were nearly eliminated and it is probable that the Drive Systems would have operated properly.

This possible solution was not load tested for three reasons:

1. Installation of the HV Capacitors was not done in a manner which would be safe for load operation.

2. Without a complete Power System Analysis, the full effect of of HV Capacitors could not be predicted, ie. System Resonances, etc.

3. The ECONODRIVE had been subjected to severe usage due to problems outside their control and should they have failed due to unapproved test modifications, ANVIC might have been held liable.

NORMAL STARTING SEQUENCE

When the drive system is first connected, i.e. the motor starter is brought in, the motor stator or primary is connected, the 2400 V side of the regenerated power transformer in the ECONODRIVE is connected but the secondary of the motor is not connected at the same instant.

The next step is to connect the secondary of the motor through the secondary contactors. This action is accomplished within one second of the primary closure.

Thereafter (approximately 20 seconds), the gating of the thyristors commences and the motor begins to accelerate.

Once the gating begins the system is under the control of the ECONODRIVE in terms specifically of speed and torque (or secondary current).

From the oscillographs it has been determined that during starting of a first drive, prior to the beginning of gating control, the 3Ø AC supplies to the System Logic Board are experiencing both shape distortion and voltage variation which, although severe, does not prevent the starting sequence from taking place properly since the disturbance is over before gating begins.

Subsequently, after the first drive system is operating, and a second drive is started, the first drive, within about one cycle of the second drives starting, trips off.

The 3 Ø, 18 VAC this time both distorts and changes level. This voltage distortion, with the first drives logic control monitoring the voltage, results in miscuing of the SCR's. The result is that the system goes overcurrent, causing the drive to trip off.

Because the second drive has not yet begun its gating control, i.e. started to accelerate the drive, its requirement for torque production has not yet begun. Accordingly, it sees no monitored disturbance since gating hasn't started and continues operation as normal, accelerating up to speed and then operating within its regulated speed settings.

"SIMULTANEOUS" START

During the tests conducted, two drives were started within approximately twelve seconds of each other.

When this was done, both drives accelerated up to speed, and ran normally. The line disturbances were still existent, but neither system had started gating before line disturbance disappeared and were thus able to start normally.

3 Ø AC LINES

The lines which were monitored during these tests were 3 phase, 18 VAC lines inside the first drive. These lines are supplied by the 600 VAC line and used by the SCR's as a reference for line commutation, and ultimately, through the feedback transformer connected to the 2400 V lines in the motor starters.

Thus it is believed that the 2400 VAC lines see the same disturbance and do not seem to be able to provide the necessary stability or "stiffness" in holding the sine wave.

PRELIMINARY CONCLUSIONS

The sequence of tests which were conducted over the last week have resulted in the following observations:

The problem of multiple drive tripping is not within the Secondary Controller. The Drive is responding to what is sees as supply line problems.

The immediate connection of the drive system to the power lines results in line disturbances.

RECOMMENDATIONS

1. The 2400V lines should be monitored in order to determine if, as is thought, there is a parallel disturbance to that seen by the Drive circuitry.

2. If the 2300V lines are distorting a distribution system analysis should be carried out to determine the necessary line stiffness required.

3. The 13.8KV - 2.4 KV transformer should be checked to determine that it is okay.

APPENDIX C
BUREAU ANALYSIS OF MANUFACTURER'S TEST DATA AND OSCILLOGRAMS

APPENDIX C

BUREAU ANALYSIS OF MANUFACTURER'S TEST DATA AND OSCILLOGRAMS (OBTAINED ON NOVEMBER 16-17, 1982)

INTRODUCTION

The second half of this appendix consists of several copies of the actual test records and oscillograms that the manufacturer submitted to the Bureau. Each oscillogram had to be touched up to provide legible copies because the manufacturer retained the originals. With each oscillogram, the manufacturer also submitted the particular circuit configuration, purpose of the test, and observations.

DESCRIPTION OF TESTS

In all of the tests, the drives were inhibited. Each test consisted of first energizing unit A and then shortly thereafter energizing unit B. Tests 1 and 4, 2 and 5, and 3 and 6 were considered and analyzed in pairs because the test configuration for each pair was identical, with the exception that plant voltages were monitored on odd numbered tests and plant currents monitored on even numbered tests. Only the oscillograms showing the second unit energization are presented in this appendix.

ANALYSIS

This section covers the analysis of the manufacturer's test data. In reviewing the analysis, please refer to the test data and oscillograms at the end of this appendix.

Tests 1 and 4

Tests 1 and 4 essentially consisted of energizing the transformer, stator, and tuned circuit together. Energizing the second tuned circuit with the first circuit preenergized resulted in a cross excitation that caused the first tuned circuit to oscillate. Sustained harmonic oscillations seemed to occur in both tuned circuits. The cross coupling was very evident in both the current and voltage waveforms.

The A unit current was severely distorted prior to energization of the second unit. This distortion can be seen in both the 2300- and 600-V bus waveforms. Energizing only the second unit contributes to the current and voltage waveform distortions. Sustained harmonic oscillations occurred in each unit and were possibly in synchronism or, if not, were at least additive, as evidenced by an increase in the sustained current harmonics in the first unit whenever the second unit was energized. It appeared that a portion of the circuit was in resonance, which produced large harmonic currents. These currents were largest when the oscillating circuit was first energized, and may be due to either transformer inrush, transformer saturation (ferroresonance), the large switching offset, or any combination thereof.

In test 1, there was a large overvoltage that occurred on the 600-V bus of the unit being started. This is characteristic of a resonant circuit and further supports the contention that the problems at Fountain Valley are related to resonance.

Tests 2 and 5

Tests 2 and 5 consisted of energizing the transformer and stator at the same time; the filter circuit was never energized. Feedback transformer inrush currents and saturation of either the current or feedback transformer occurred. There was no generation of harmonics or cross coupling of the saturation effects.

It is interesting to note that the transformer inrush current peaks obtained in test 5 were larger than those of test 4. However, the corresponding voltage distortion due to the larger inrush current was not larger in test 5 as would be expected, but was actually smaller. This strongly suggests the problem to be other than inadequate system impedance, and is probably related to the sustained oscillations that occur when the filter is in the circuit.

Tests 3 and 6

During tests 3 and 6, both unit transformers and stators were energized, then the tuned circuits were individually energized. When the first tuned circuit was energized, waveform distortions occurred but were essentially damped out in three to four cycles. Energizing the second tuned circuit resulted in the same three- to four-cycle distortion; however, this distortion was cross coupled into the previously energized tuned circuit and created current oscillations in the circuit. Only minor voltage distortions were induced into the other previously energized tuned circuit.

Preenergization of the unit stator and transformer minimizes the disturbance. There is still a large current oscillation, but it decays rather quickly. The current distortion prior to energization of the second unit was also less than in test 4. Obviously, eliminating the magnetic inrush greatly reduces the problem. This may be due to elimination of the inrush current effects, transformer saturation (ferroresonance), the large transformer switching offset, or any combination thereof. The results of these tests indicate the problem is resonance related, and not related to inadequate system strength.

As in test 1, there was a large overvoltage that occurred on the 600-V bus of the unit being energized, which further supports the contention that the problem is related to resonance in the tuned circuit.

CONCLUSIONS

Energizing the tuned circuit, transformer, and stator together results in sustained harmonic oscillations. Obviously, the near 180-Hz oscillations were generated from the harmonic filter, which was designed to filter bridge harmonics. The oscillations decayed in three to four cycles when the tuned circuit was energized separately from the transformer and stator. Based on these facts, two observations can be made: (1) oscillations in the first case are very underdamped as compared to the overdamped second case, and (2) as observed from tests 2 and 5, the transformers have large inrush currents and possibly a high degree of saturation. These two observations result in either of the two following conclusions:

1. Energizing the entire circuit simultaneously results in a tuned circuit with an extremely large resonance peak centered near 180 Hz, the third harmonic frequency.

2. The saturation and inrush current of the transformers combined with the resonance problem of the near third harmonic filter may result in sustained harmonic oscillations due to transformer ferroresonance effects.

The ferroresonance effect should be investigated further. It seems unlikely that the circuit resonance, which is already high since it is a low resistance circuit, can increase so much when being energized with the transformer.

One possible solution to the problem may be to energize the transformer and stator separately from the tuned circuit and then wait about 10 to 50 cycles before energizing the tuned circuit. This would still result in three to four cycles of induced current harmonics, but at a reduced level. The 60-cycle voltage waveform distortions due to the third harmonic requirements of the transformer would then be greatly reduced and may be sufficiently low enough to allow proper system operation. Also, if ferroresonance is occurring, it may be fairly easy to damp it out with a transformer damping resistance.

Investigate the transformer configuration (i.e., is it y-y and would y-Δ help reduce the harmonic distortions).

Stiffness of the 2300-V system is not the problem. The problem is that there are two tuned circuits that are tied together through a very low impedance (i.e., transformer impedance). If ferroresonance is involved, or if the transformer inductance plays a part in tuning the circuit, there is very little impedance between circuits and the cross coupling effect is predictable. In any event, there are two tuned circuits coupled through two transformers with very little loss in the circuit. Large current magnitude oscillations in one tuned circuit will produce slight voltage disturbances that will be transferred to the other tuned circuit and will induce large current oscillations (i.e., swapping of vars) between the two circuits. In this case, as is typical in power systems, this is very sensitive to the impedance between the tuned circuits. Trying to eliminate the effect by trying to create an infinite bus at the 2300-V system level is simply a brute force method of solving the problem.

TEST DATA AND OSCILLOGRAMS SUBMITTED TO BUREAU OF RECLAMATION

(NOTE: The edited text extractions shown in this portion of appendix C were obtained from the consultant's report.)

TEST 1 – NOVEMBER 16, 1982

Purpose

The purpose of test 1 was to determine the voltage disturbance on the 600-V lines inside the drive due to transient inrush upon the starter connection of drive A initially, and then while drive A was connected, the connection of drive B.

Observations

Five tests were run, and in each test upon connection of drive B, the 600-V lines were distorted to such a degree that the converter of drive A could not have commutated properly. It was also noted that upon immediate connection of drive A, the building lights dimmed or "flickered" indicating system (2300-V) voltage variation. The magnitude of the voltage variation was not measured accurately but appeared from the oscillograph traces to be about 10 to 20 percent.

TEST 2 – NOVEMBER 16, 1982

Purpose

The purpose of test 2 was similar to that of test 1 except that the effect of the power factor capacitors in the a-c reactor filter circuit was removed.

Observations

Three tests were run and, although the 2300-V variation (dip) and the 600- and 2300-V line harmonics were observed in each test, the distortion caused by connection of the second drive was not significant. This indicated that through the removal of all power factor capacitors and a-c reactors, the transient inrush current could be reduced to a level which would, in all probability, allow the first drive on-line to continue operating; or that the filter network was itself creating the disturbance, thereby distorting the system voltages to the point of commutation failure. Obviously, we cannot run a drive in such a configuration; i.e., without a filter network, because the harmonics would be too high and the power factor would be too low.

TEST 3 – NOVEMBER 17, 1982

Purpose

The purpose of test 3 was to determine the effect of the filter network by itself on the 600-V lines in terms of voltage distortion.

Observations

Four tests were run and, in each test, the result of switching the networks showed no significant effect, which indicated that the filter network was (in itself) not creating the line disturbance.

TEST 4 – NOVEMBER 17, 1982

Purpose

The purpose of test 4 was to determine the level of current inrush on the 2300-V side of the feedback transformer upon starter connection of initially drive A, and then drive B.

Observations

This test showed that much of the required transient or inrush current of the second system was supplied by the transformer of drive A. Although it was expected that the current would be approximately in proportion to the kilovolt-ampere ratio of the feedback transformer to the distribution plus feedback transformers; i.e., 200/2700 or 7.5 percent would be drawn from drive A. It appears that the current draw was significantly higher than this.

It was also observed that the kilovolt-ampere inrush to the system was about 175 A at 2300 V, or 400 kV·A. This was well below inrush levels that would be considered excessive; e.g., indicating feedback transformer problems. Steady-state current was about 50 A, as measured by the instrumentation on the 2300-V starter. The preliminary interpretation was that the 2300-V line was not "stiff" enough to provide the current inrush required by the second system coming on line.

TEST 5 – NOVEMBER 17, 1982

Purpose

The purpose of test 5 was to measure the magnitude of only the transformer inrush.

Observations

The level of inrush to the feedback transformers was not excessive, nor was the time taken for the core to be magnetized (about 0.5 second). Both of these parameters were well within acceptable limits, and showed no transformer problems.

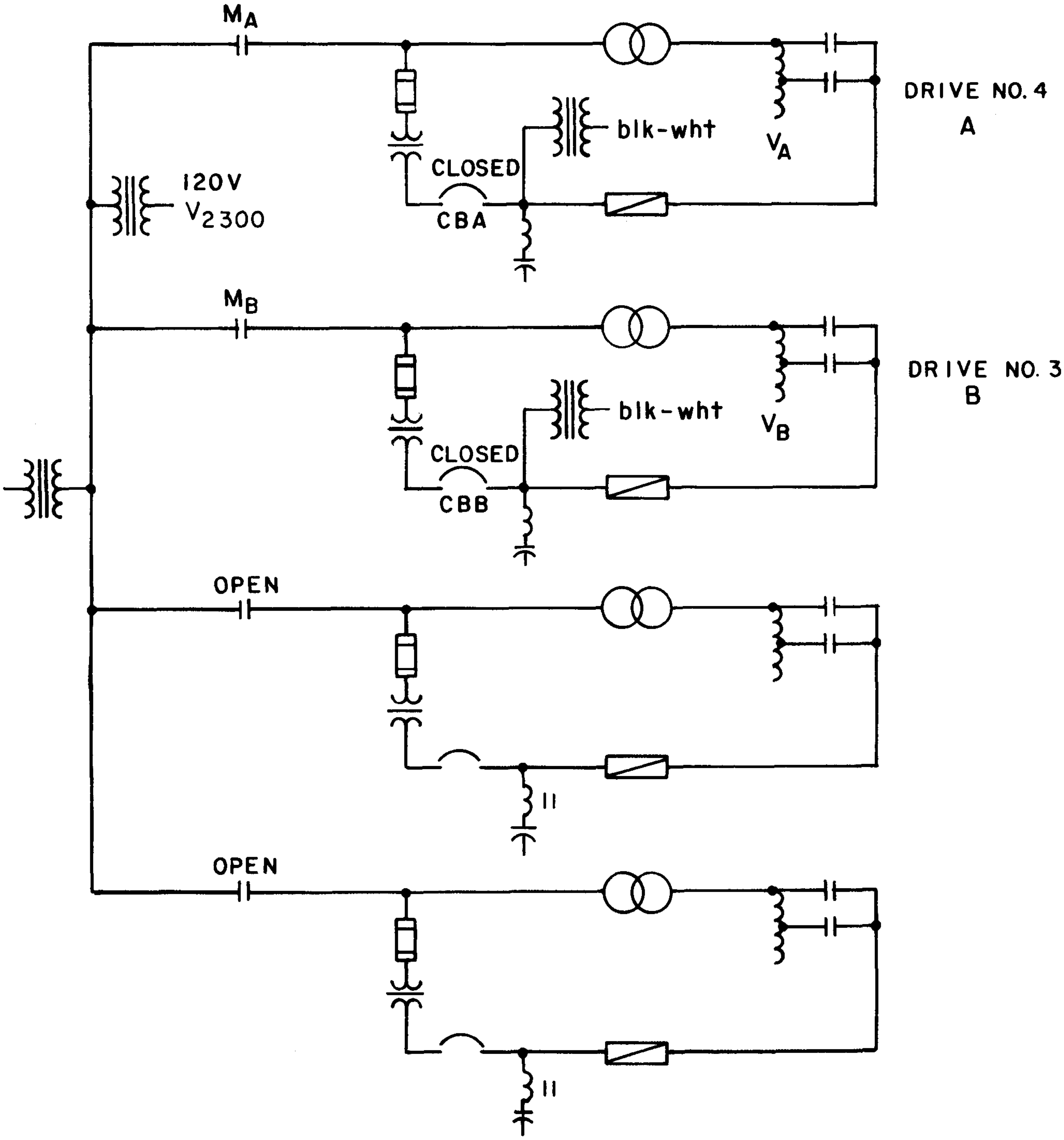
TEST 6 – NOVEMBER 17, 1982

Purpose

The purpose of test 6 was to observe the transformer currents when the power factor capacitor in the a-c reactor filter network was switched in separately.

Observations

From the results of test 4, switching the second filter network in draws more of the inrush current from the transformer on line than expected, causing disturbance within the on-line system.



NOTES: 1. GATES OFF AT ALL TIMES (DISCONNECTED S/S (No.12 SLB-1)
2. SEPARATE PT 2300:120 FOR V_{2300}
3. TEST NO. 1a CLOSE M_A
4. TEST NO. 1b CLOSE M_B WHILE M_A ALREADY CLOSED

Figure C-1. – Test No. 1 system configuration.

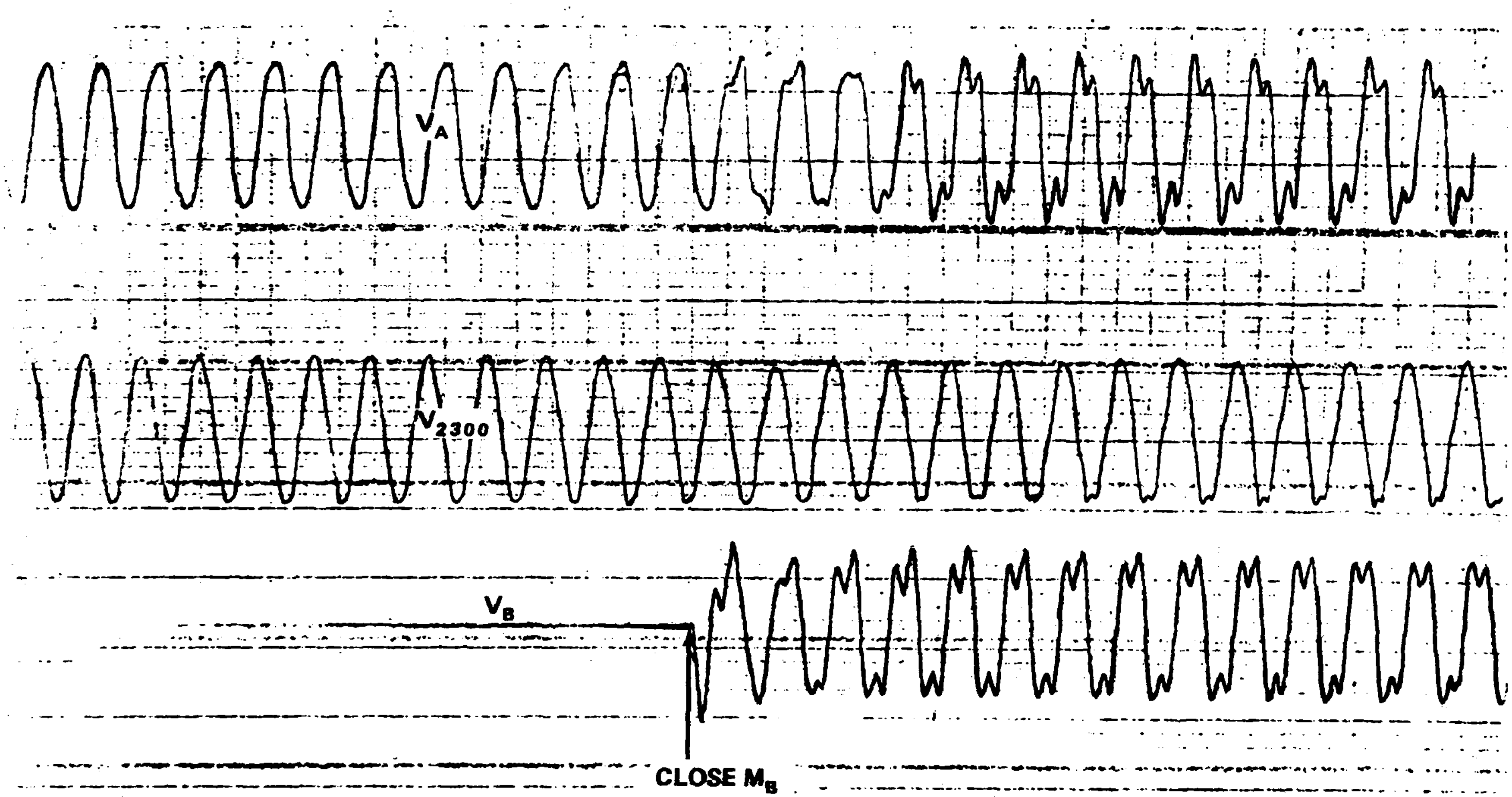
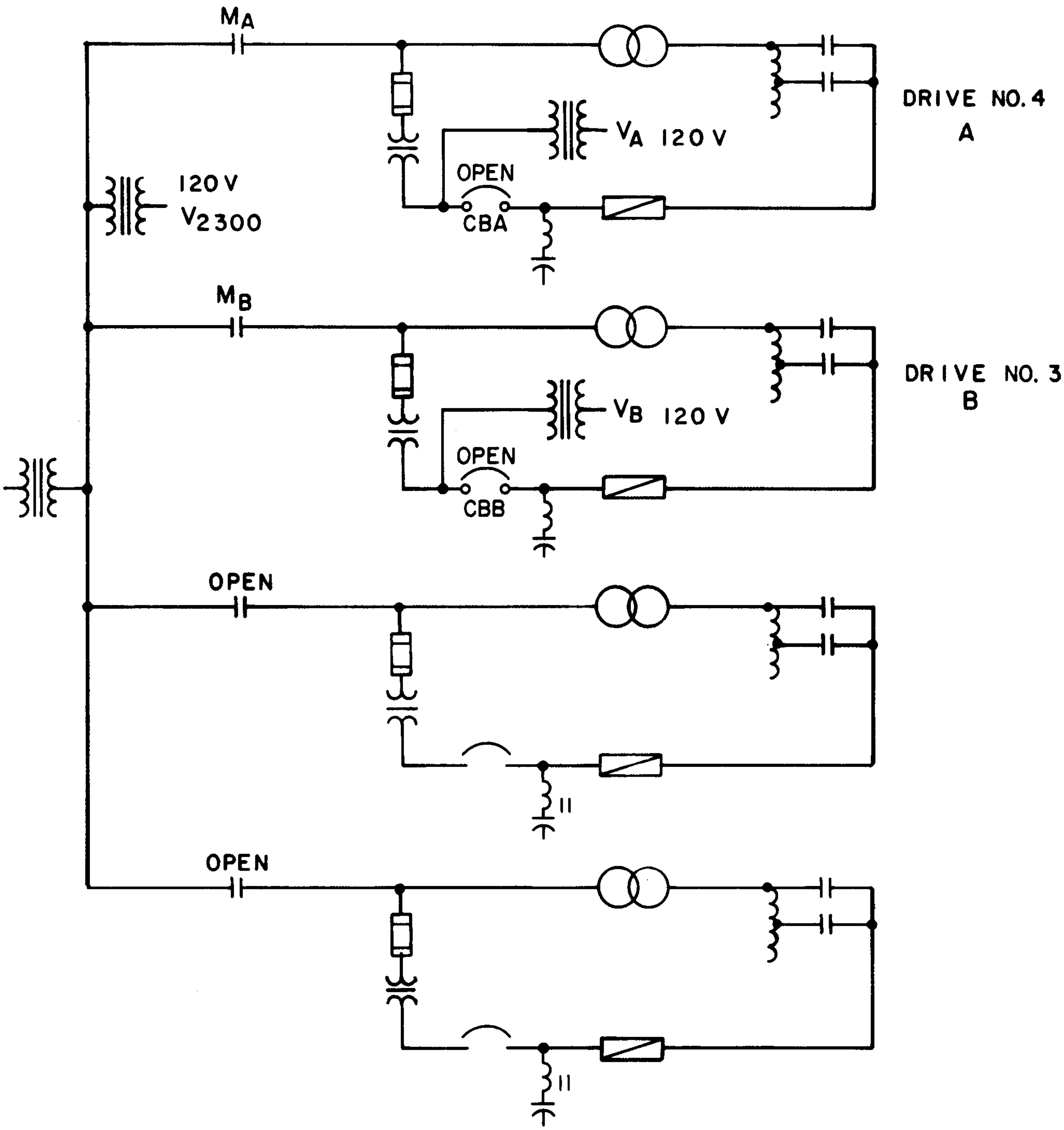


Figure C-2. – Test 1B oscillogram.



NOTES: 1. GATES OFF AT ALL TIMES

2. CIRCUIT BREAKERS CBA AND CBB OPEN AT ALL TIMES

3. TEST NO. 2a - CLOSE M_A

4. TEST NO. 2b - CLOSE M_B WHILE M_A ALREADY CLOSED

Figure C-3. – Test No. 2 system configuration.

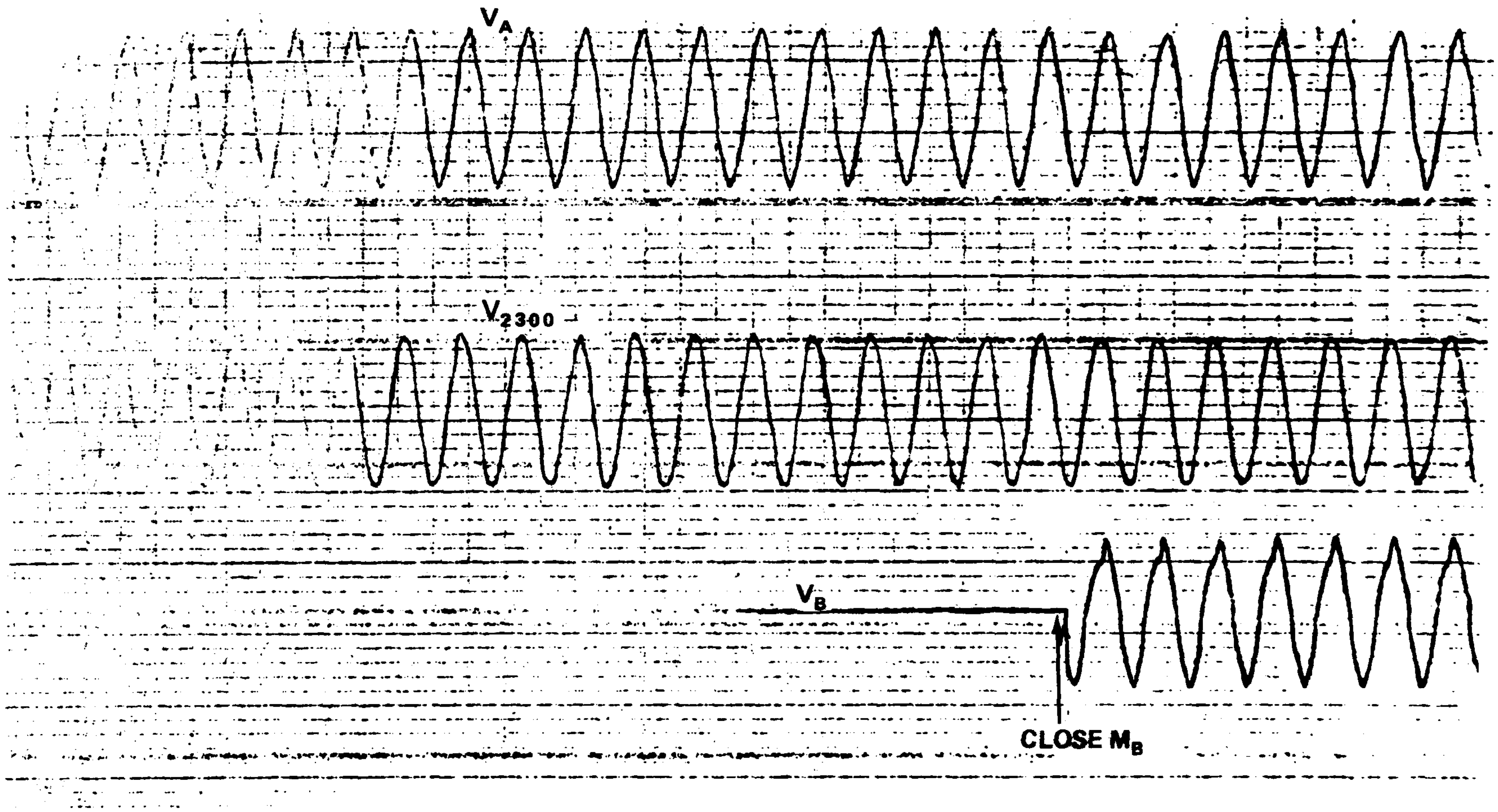
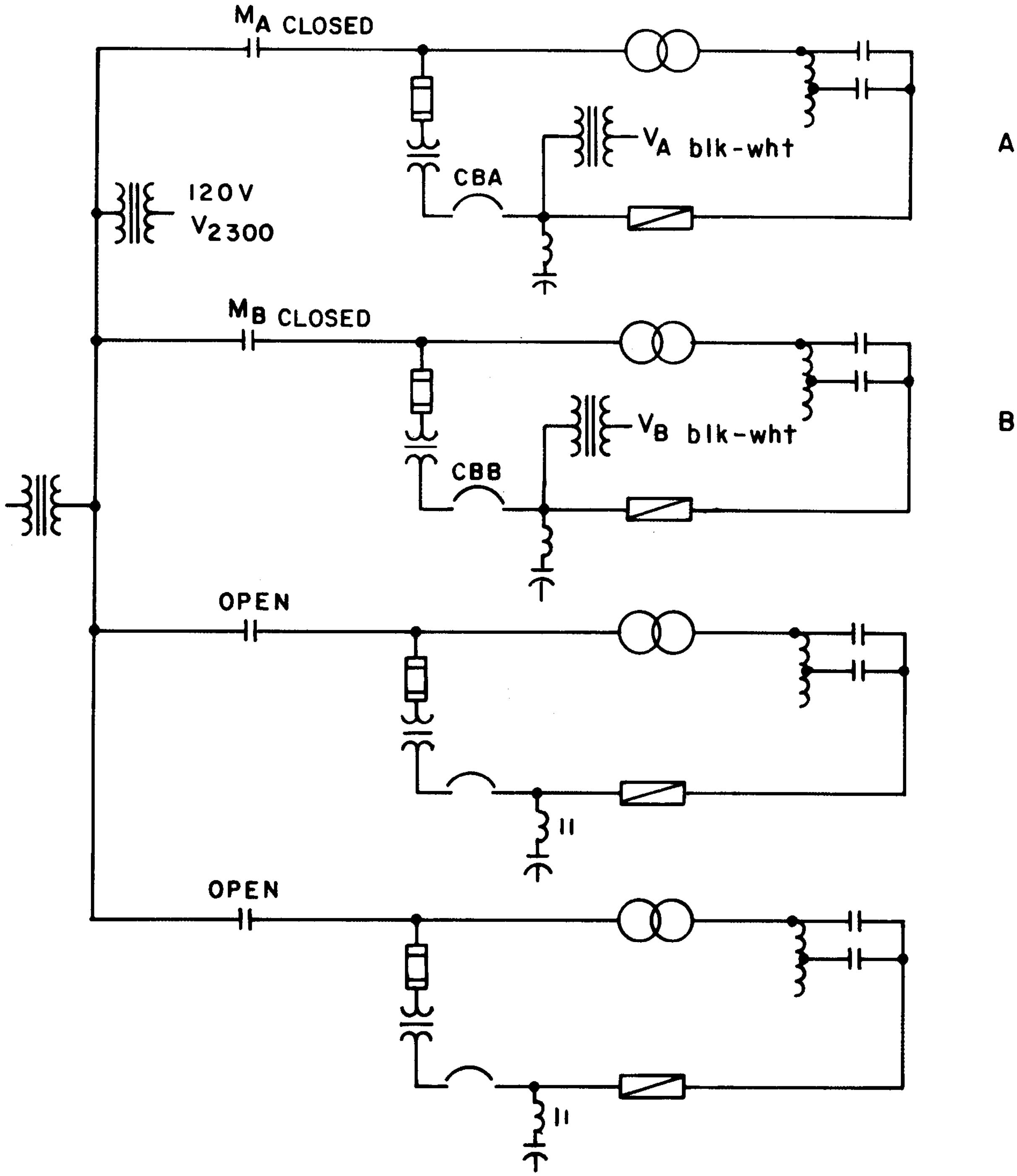


Figure C-4. – Test 2B oscillogram.



NOTES: 1. GATES OFF AT ALL TIMES
2. SEPARATE PT 2300 : 120, M_A AND M_B CLOSED AT ALL TIMES
3. TEST NO. 3a - CLOSE CBA
4. TEST NO. 3b - CLOSE CBB WHILE CBA IS CLOSED

Figure C-5. – Test No. 3 system configuration.

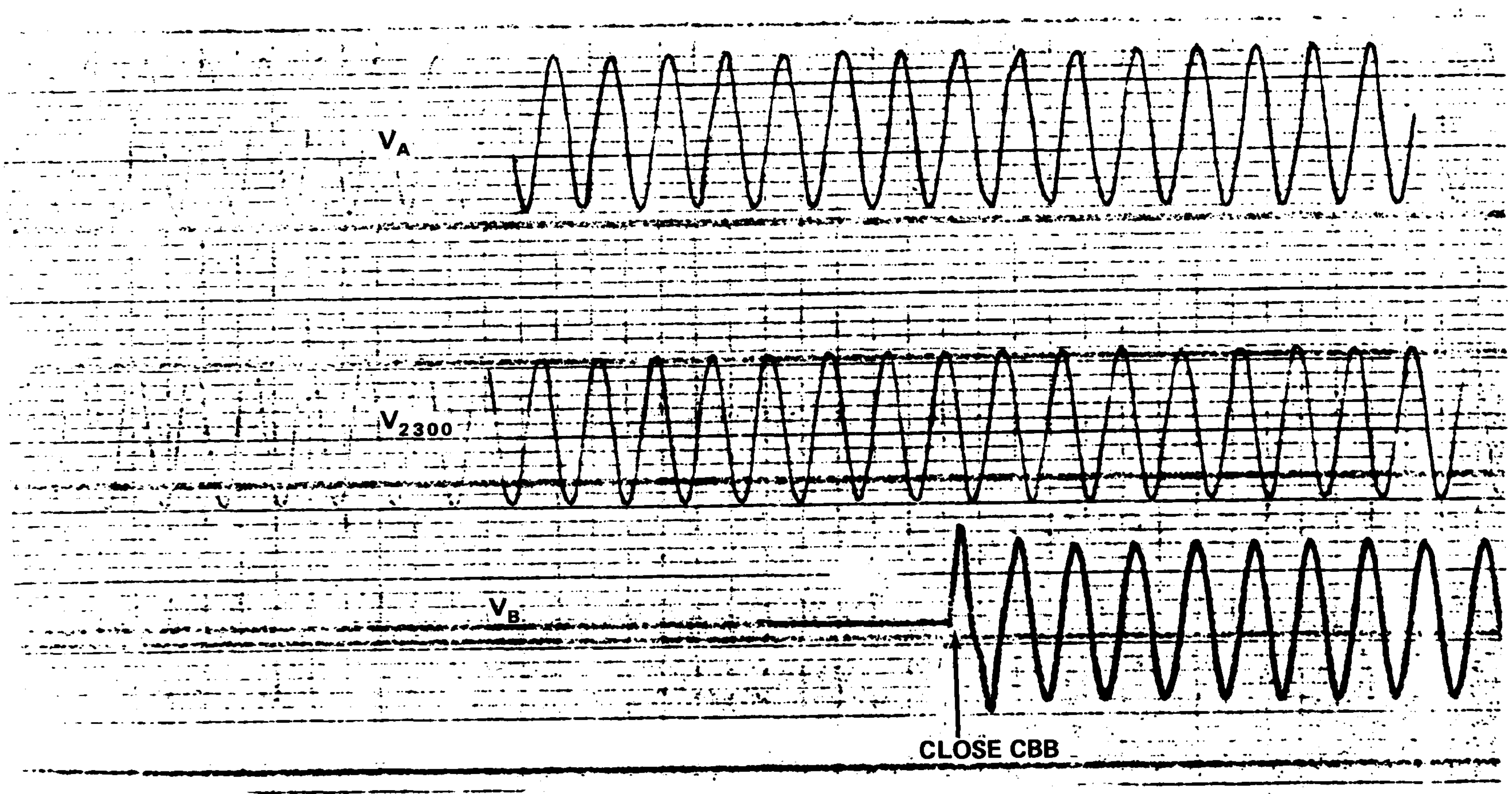
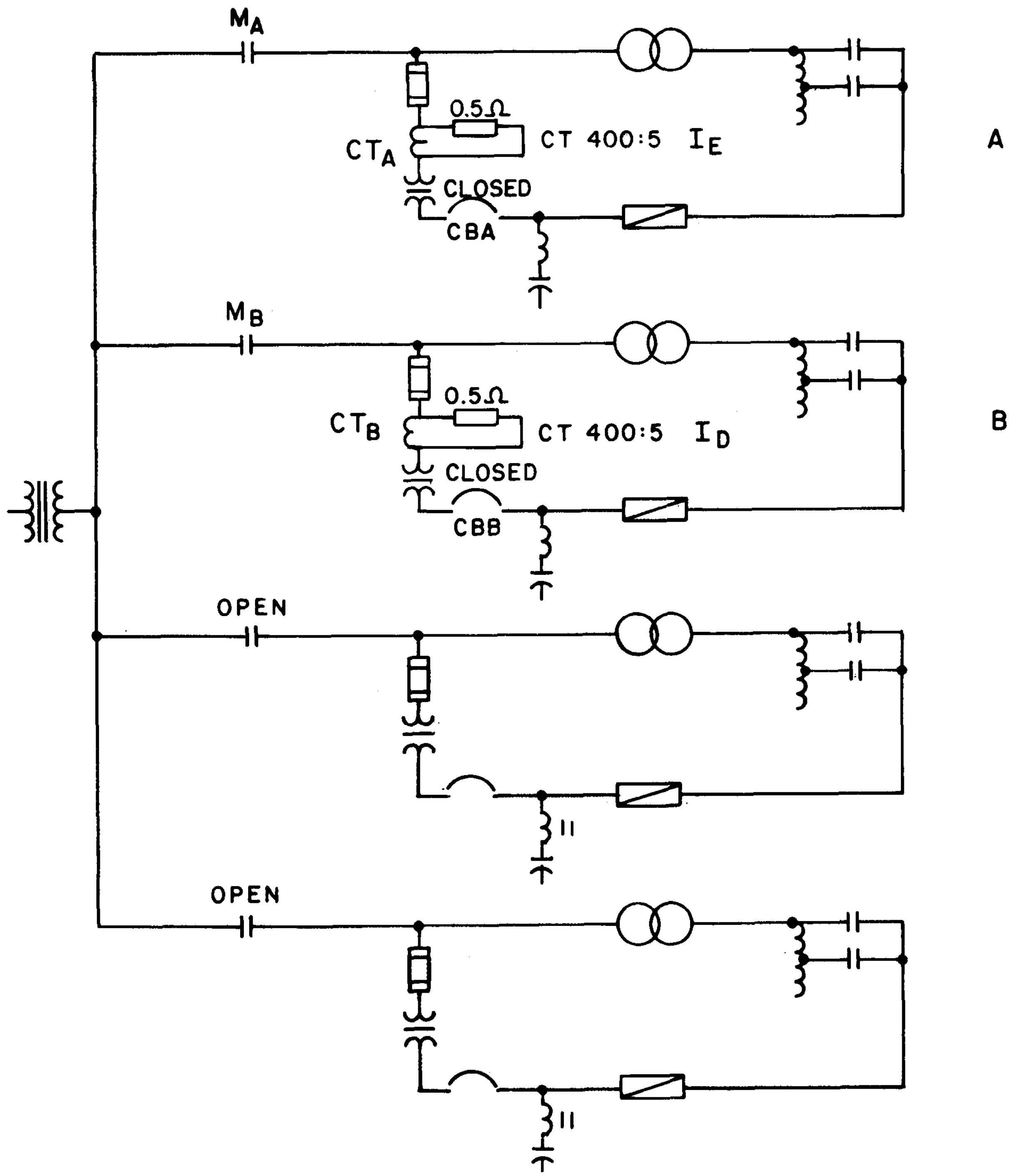


Figure C-6. – Tests 3A and 3B oscillogram.



NOTES: 1. I_E ACROSS CT_A, I_D ACROSS CT_B
2. CBA AND CBB CLOSED AT ALL TIMES
3. TEST NO. 4a-CLOSE M_A
4. TEST NO. 4b-CLOSE M_B WHILE M_A CLOSED

Figure C-7. – Test No. 4 system configuration.

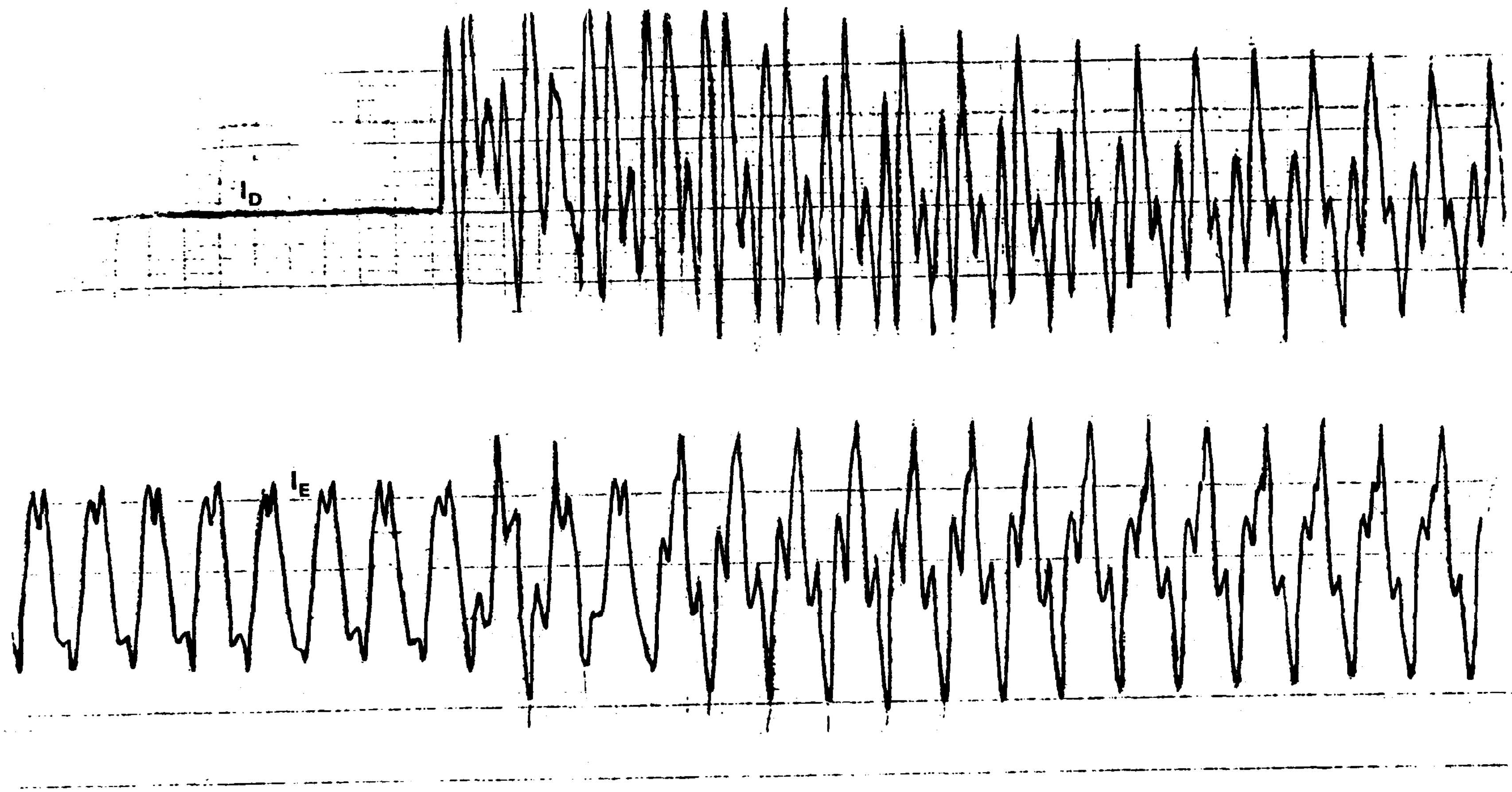
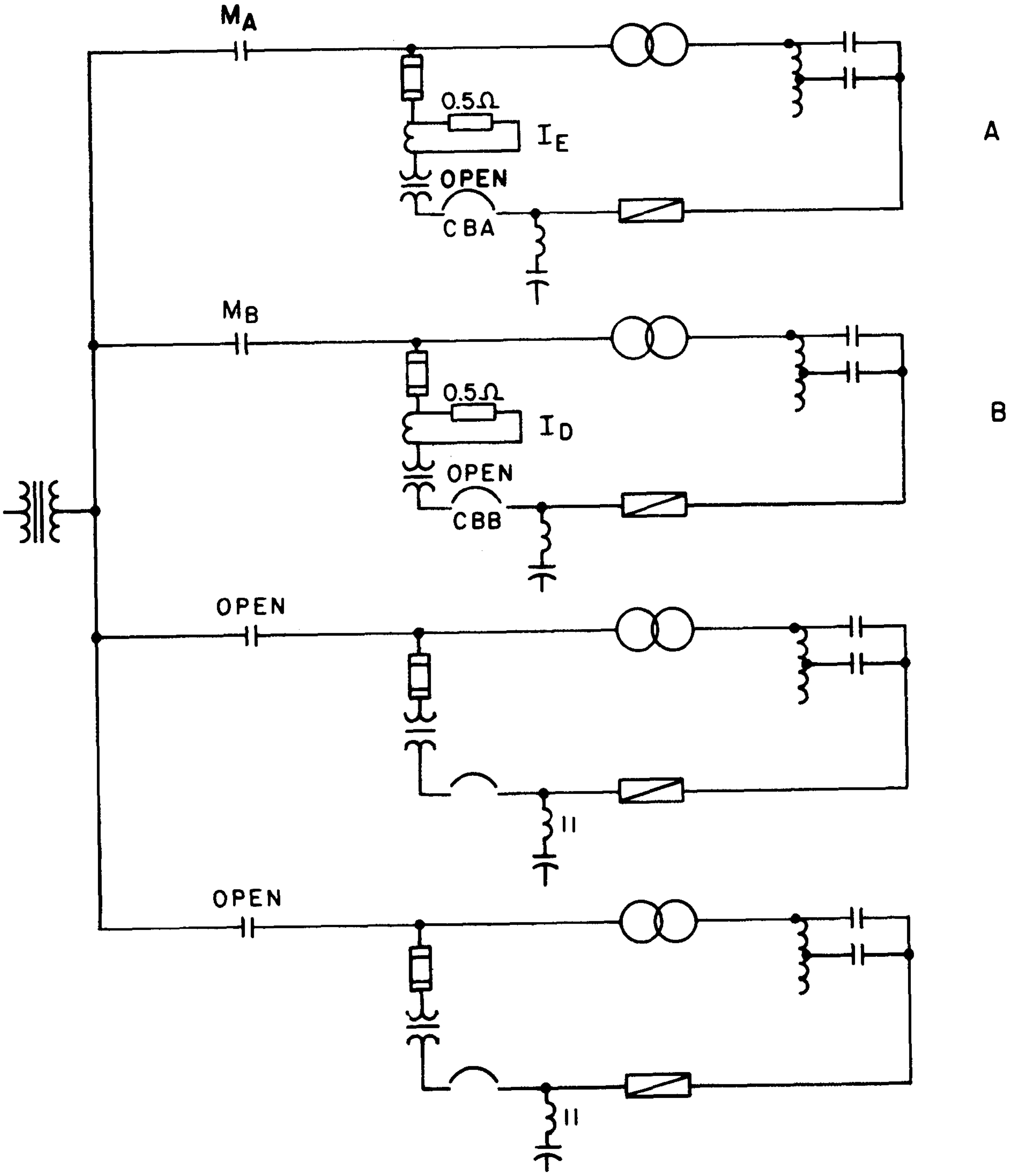


Figure C-8. – Test 4B oscillogram.



NOTES: 1. CBA AND CBB ALWAYS OPEN AND GATES OFF
2. TEST NO. 5a - CLOSE M_A
3. TEST NO. 5b - CLOSE M_B AFTER M_A ALREADY CLOSED

Figure C-9. – Test No. 5 system configuration.

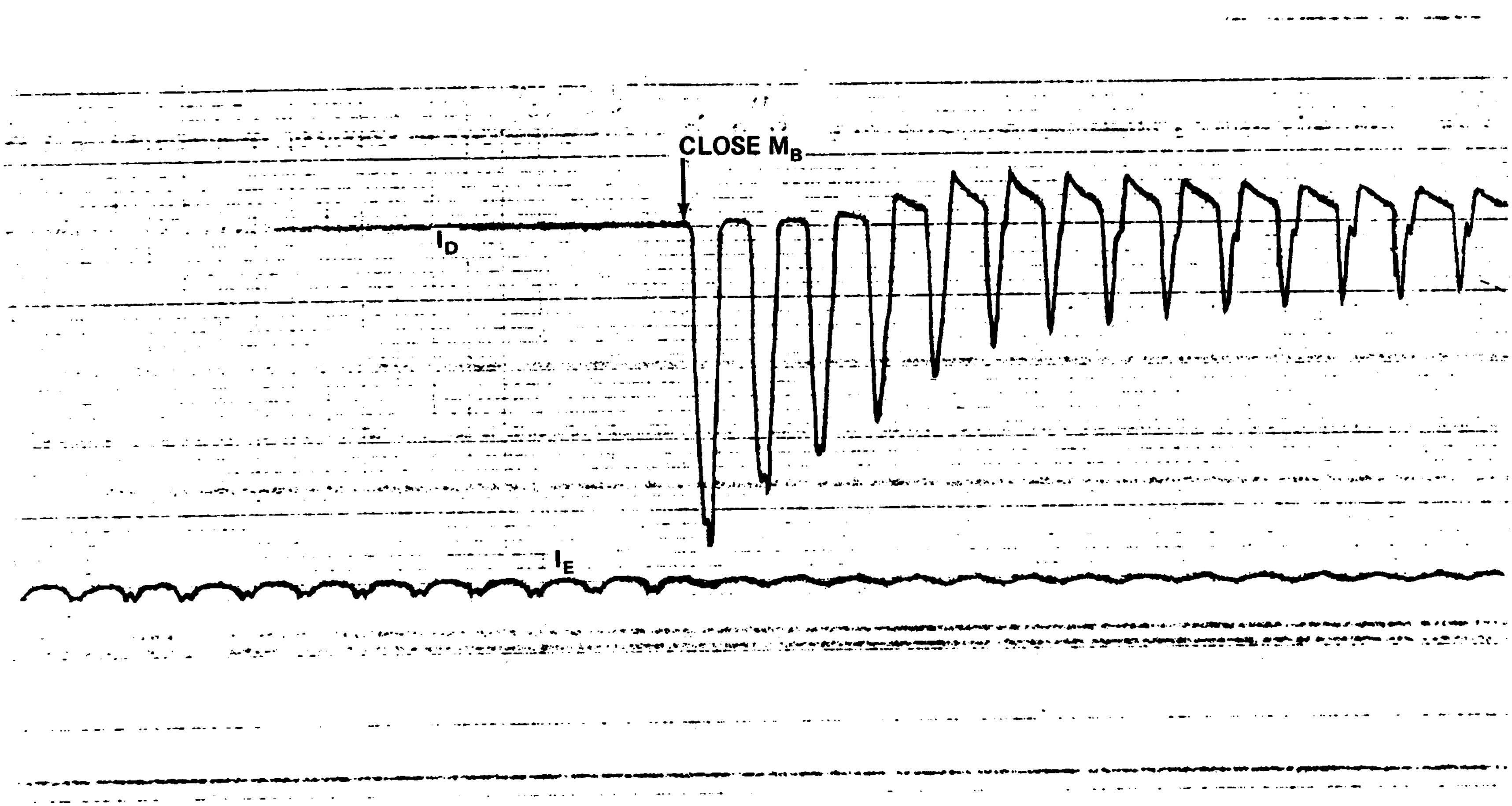
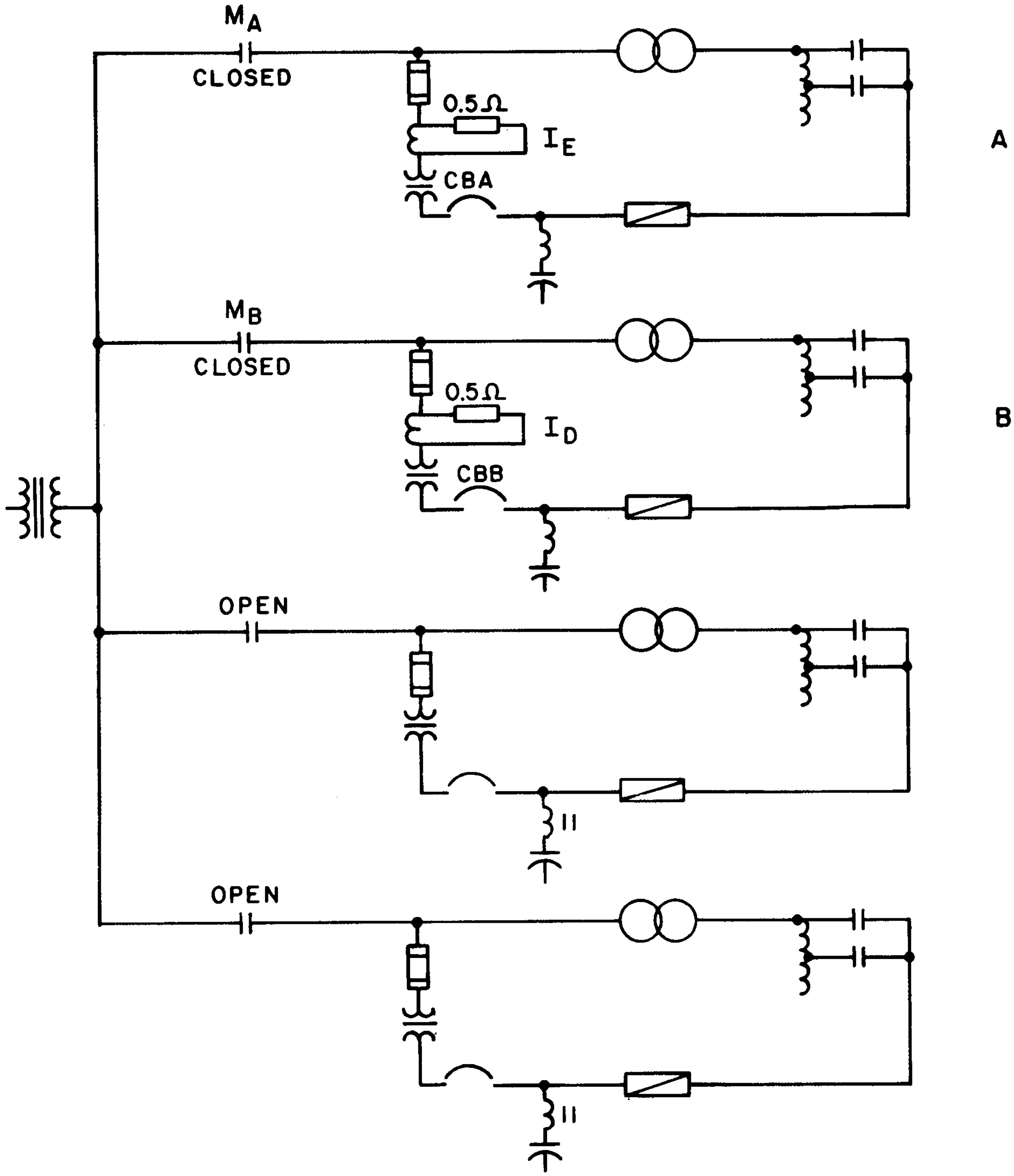


Figure C-10. – Test 5B oscillogram.



NOTES: 1. GATES OFF AT ALL TIMES
2. M_A AND M_B CLOSED
3. TEST NO. 6a – CLOSE CBA
4. TEST NO. 6b – CLOSE CBB WHILE CBA ALREADY CLOSED

Figure C-11. – Test No. 6 system configuration.

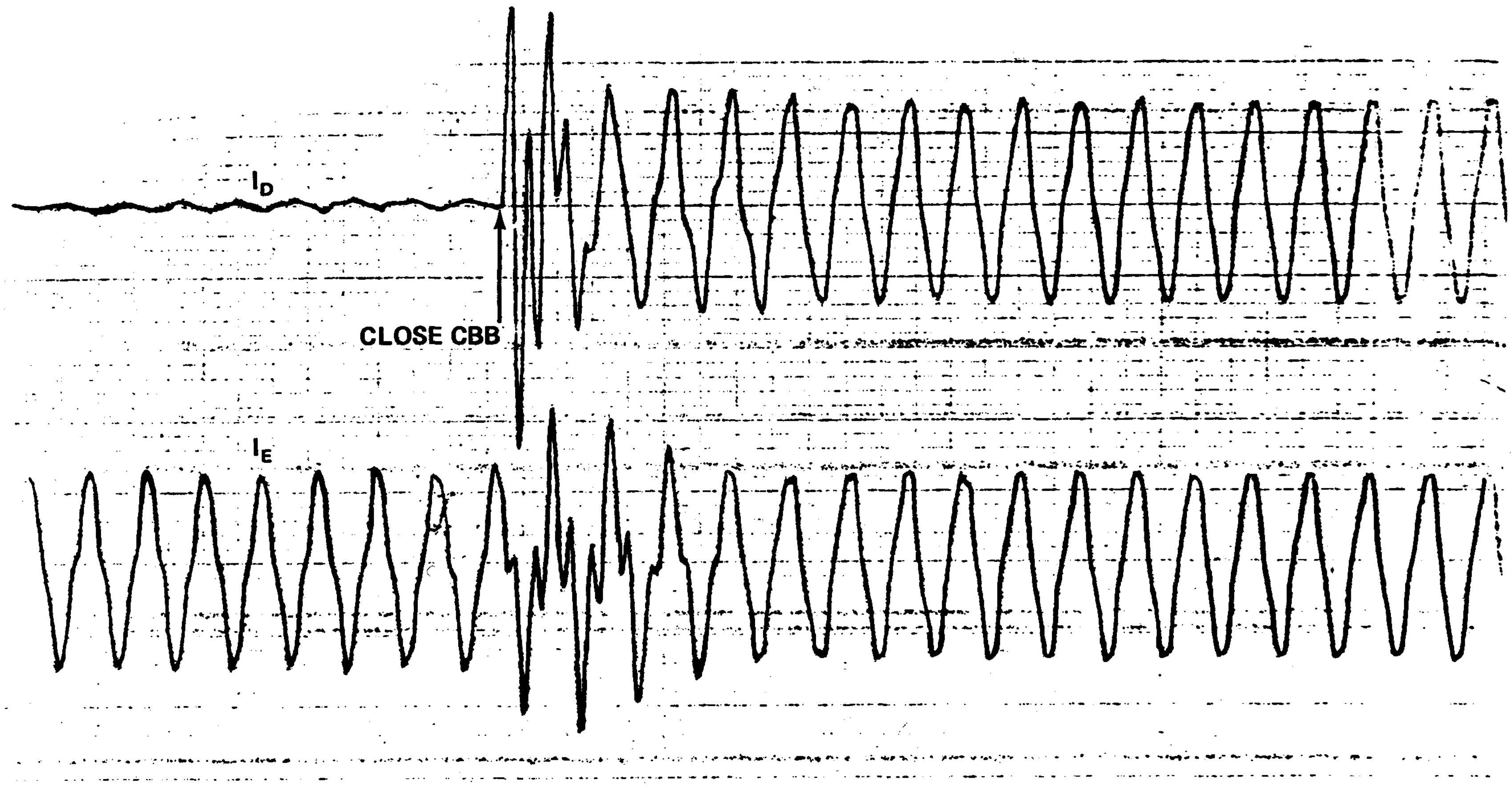


Figure C-12. – Test 6B oscillogram

APPENDIX D

NOTES ON ANALYSIS OF TEST OSCILLOGRAMS

INTRODUCTION

This appendix is an analysis of the Fountain Valley tests performed on February 22 and 23, 1983. The text is in note form and was written as commentary for use in conjunction with or after reading the consultant's report entitled, "Variable Speed Drive System Analysis and Test Report," dated March 18, 1983.

TEST 1

Initial Conditions

The drive No. 4 filter was set at 170 kΩ and 320 μH (251 Hz).

Intent

To energize the starter, feedback transformer, and filter of circuit No. 4, and observe the cross excitation of circuit No. 3.

Consultant's Conclusion

Four out of five tests appeared satisfactory.

Bureau Comments

1. Not all three phases of the 600-V circuit were monitored; therefore, depending on the point-on-cycle time of energization, it cannot be fully determined if energization of all three phases was acceptable.

2. Test data observations were:

Test A. – Extensive distortion in the 600-V No. 4 circuit and extensive cross excitation of circuit No. 3 occurred. This energization was unacceptable.

Test B. – An acceptable energization of circuit No. 4 for the phase that was monitored; however, the other two phases were not monitored. The fact that some distortion occurred in the circuit No. 3 voltage trace leaves a question as to the success of this attempt.

Test C. – The No. 4 circuit 600-V trace was somewhat distorted initially. Circuit No. 3 was acceptable; however, the other two phases were questionable.

Test D. – Same at test C.

Test E. – Extensive distortion of circuit No. 4 voltages and extensive cross excitation of circuit No. 3 occurred. This attempt was judged unsuccessful.

Bureau Conclusions

We agree with the consultant that the lower the kilovolt-ampere reactive rating is, the lower the charging requirements will be. However, we believe (without observing the other phases) that only two or three of the tests appeared satisfactory from a cross excitation point of view.

What has not been mentioned is the magnitude of the harmonic current observed when the transformers and filters are energized. This harmonic current is present in the 2300-V line currents (i.e., the system side of the feedback transformer). We believe that the filter on the inverter side of the transformer was tuned to about 250 Hz; this does not include the effect of the stator core. The fact that there is a large, harmonic current in the 2300-V lines of the delta-connected feedback transformer bank that is obviously capable of producing sustained cross excitation does in fact indicate an unusual mode of oscillation, perhaps due to either a detuning of the filter or as a result of ferroresonance. Normally, inrush currents are about 8 to 12 times the rated current, and decay in three to five cycles. This is not what is occurring here. The following observations should also be noted:

1. The size of the filter capacitors will always be of sufficiently low reactance to induce a swapping of vars between different motor circuits regardless of the external system strength. The swapping of vars is more or less determined by the impedance separating the individual var banks.

2. During these tests, the transformers were loaded with the large capacitive var load.

3. The 2300-V bus voltages are essentially unaffected by what is happening in the 600-V circuits both with respect to waveform and magnitude; i.e., vars are being swapped and there is sufficient system strength to maintain system voltage and waveform.

4. There are sustained, harmonic components in the line currents of the delta-star connected feedback transformer bank. The delta is on the 2300-V side of the bank. Therefore, we conclude that the observed harmonic components in the lines may be very small compared to the circulating third harmonic current in the delta.

Additional Information Required

Current magnitude scales and stator winding information with respect to the effect on the filter frequency.

TEST 2

Initial Conditions

The drive No. 4 filter was set at 160 kΩ and 320 μH (259 Hz).

Intent

The objectives and procedures are the same as for test 1, only at reduced charging current levels.

Consultant's Conclusions

Less charging current is required due to the reduced capacitance. Of the 10 tests run, 9 were acceptable and 1 was marginal.

Bureau Comments

1. As in test 1, only one phase was monitored, which makes it difficult to comment on the other two phases.

2. Test data observations were:

Tests A, C and I. – No cross excitation in phases monitored.

Tests B, E, F, G, H, and J. – Questionable degree of cross excitation.

Test D. – Extensive cross excitation occurred.

Bureau Conclusions

We agree that the charging current has been reduced, but our conclusions on test 1 regarding the harmonic line currents are still valid for test 2.

TEST 3

Initial Conditions

The drive No. 4 filter was set at 220 kΩ and 250 μH (249 Hz).

Intent

The objectives and procedures are the same as for tests 1 and 2, only at specified charging current levels.

Consultant's Conclusion

Of the five tests run, two were acceptable, one was marginal, and two were unacceptable.

Bureau Comments

1. As in tests 1 and 2, only one phase was monitored, which makes it difficult to comment on the other two phases.

2. Test data observations were:

Test A. – Extensive distortion in the 600-V No. 4 circuit and extensive cross excitation of circuit No. 3 occurred. This energization was unacceptable.

Test B. – Extensive distortion occurred in the 600-V No. 4 circuit. The record submitted ended shortly after energization of circuit No. 4 and, as a result, we could not tell if cross excitation occurred; it sometimes takes several cycles for cross excitation to develop.

Tests C, D, and E. – Same comments as given for test A.

Bureau Conclusions

Four of the five tests were unacceptable and one was marginal. In reviewing tests 1, 2, and 3, the series of tests in test 3 generally had a predominate third harmonic component significantly larger than that observed in tests 1 and 2. It almost appears as if the 220-kΩ filter is the critical or threshold level at which the performance of the circuit quickly degenerates.

TEST 4

Initial Conditions

The drive No. 4 filter was set at 220 kΩ and 250 μH (249 Hz). The feedback transformer was connected directly across the 2300-V bus.

Intent

To determine the effect of startup when the No. 4 transformer inrush is eliminated from the energization process.

Consultant's Conclusion

All five tests were acceptable.

Bureau Comments

1. As in previous tests, all phases were not monitored.

2. Test data observations were:

Tests A, B, C, D, and E. – In general, the harmonic line current was small in comparison to that observed in tests 1, 2, and 3. In addition, the

600-V circuit voltage waveforms were not affected. No significant distortion could be seen in the No. 3 or No. 4 600-V waveforms.

Bureau Conclusions

The information given under "Bureau Comments" supports our contention that transformer inrush is exciting and/or cross exciting the circuits.

SECOND DAY OF TESTING (OPTION 2)

Initial Conditions

The drive No. 4 filter was set at 160 kΩ and 320 μH (259 Hz).

Option 2, Tests 1 through 4

The No. 4 motor was on-line and running, then circuit No. 3 was energized and followed by bringing No. 3 drive and motor on-line. Tests 1 and 3 were successful, but 2 and 4 were not.

Observations

Option 2, Test 1. – The 600-V unit No. 3 and No. 4 voltages were distorted. The 2300-V circuit had one small distortion that lasted from one-half to one cycle on one phase only. This occurred during the initial transformer current inrush period.

Option 2, Test 3. – The 600-V No. 3 and No. 4 voltages were distorted considerably more than during test 1. The 2300-V third harmonic line currents were also considerably larger (nine divisions in test 3 versus six in test 1). In addition, the 2300-V bus voltages were somewhat more distorted than in test 1. Please note that we refer to the voltage change as a distortion and not as a dip. On examining the records, it can be seen that the positive going portion of the waveform is not affected. The distortion occurs only during the negative portion of the waveform and at the same time that the third harmonic line currents peak. This indicates that the rather large third harmonic currents are the cause of the 2300-V distortions, refer to the oscillogram shown on figure D-1 for details. This copy of the oscillogram had to be touched up for purposes of reproduction because we did not receive the original from the consultant.

Option 2, Test 2. – The consultant's report indicates this attempt was unsuccessful, but we were unable to determine any result from the record. There was cross excitation and distortion in the No. 3 and No. 4 600-V circuits. The third harmonic currents caused a slight distortion in the 2300-V system similar to the results of test 3.

Option 2, Test 4. – This attempt was also unsuccessful. There was cross excitation in the 600-V circuits, some distortion in the 2300-V system due to harmonic influences, and a collapse of voltage in both the 2300- and 600-V circuits due to cross firing of the silicon-controlled rectifiers (line-to-line faults due to inadvertent firing of inverter silicon-controlled rectifiers).

The consultant indicates the attempts in tests 2 and 4 failed due to distortion in the 600-V circuits and as a result of a significant voltage drop on the 2300-V line. We believe the disturbance in the 2300-V circuit was induced by the third harmonics and as a result of line-to-line faults due to improper inverter operation.

TEST 3A – OPTION 2

Initial Conditions

The filter was set to 160-kΩ capacitance and 320 μH (259 Hz). Unit 4 was on-line and running when the unit 3 transformer only (no filter) was energized. This energization was repeated six times; the sixth attempt was unsuccessful.

Bureau Comments

Only record No. 6 was submitted to the Bureau. In this test, it was obvious there was cross coupling between the 600-V circuits. There was also a short duration distortion in the 2300-V supply due to the large transformer inrush current. Note that within one cycle the inrush damped out and there was no sustained harmonic oscillation.

TEST 4 – OPTION 2B

This test was changed to test 5 – option 2B after technical problems occurred in the operation of the control circuits.

TEST 5 – OPTION 2B

Initial Conditions

The drive No. 4 filter was set at 220 kΩ and 250 μH (249 Hz). All of the feedback transformers were tied directly across the 2300-V bus. Unit 4 was put on-line successfully, then unit 3 was put on-line, also successfully. Finally, unit 2 was started, at which time unit 3 tripped off.

Consultant's Conclusions

It was concluded that this failure was due to a severe voltage drop on the 2300-V line of about 10 percent or more.

Bureau Comments

Refer to the oscillogram shown on figure D-2.

Start of Unit 4. – No record of start provided.

Start of Unit 3. – No record of start provided.

It appears that only steady state records were provided for the starts of units 3 and 4.

Start of Unit 2. – Distortion was observed in both the No. 3 and No. 4 600-V circuit waveforms. It was obvious that a huge current in the line to the No. 3 drive occurred shortly after unit 2 was energized. This current distorted the 2300-V waveforms, saturated the current transformer, and was actually the result of a line-to-line fault in drive no. 3. The fault collapsed the 600-V system voltage and induced a drop in the 2300-V system voltage. For details, refer to the oscillogram on figure D-2. This copy of the oscillogram had to be touched up to provide a legible copy for this report.

Bureau Conclusions

It appears that the consultant overlooked several details. If all transformer banks were wired directly to the 2300-V bus, then we could probably (but not conclusively) rule out ferroresonance, but not tuned circuit oscillations or, as the consultant suggests, high frequency transients causing the silicon-controlled rectifier drives to misfire.

BUREAU COMMENTS ON CONSULTANT'S RECOMMENDATIONS

Consultant's Recommendation A

This recommendation consisted of isolating the power equipment from the electronic equipment. This seems reasonable and is in line with standard practices involving power electronic equipment; however, we are not sure this is a solution to all of the problems.

Consultant's Recommendation B

This recommendation was for Southern Colorado Power to provide an accurate assessment of the short circuit capability. The consultant also stated they enclosed their analysis of the system fault capability, but made no comments on their findings.

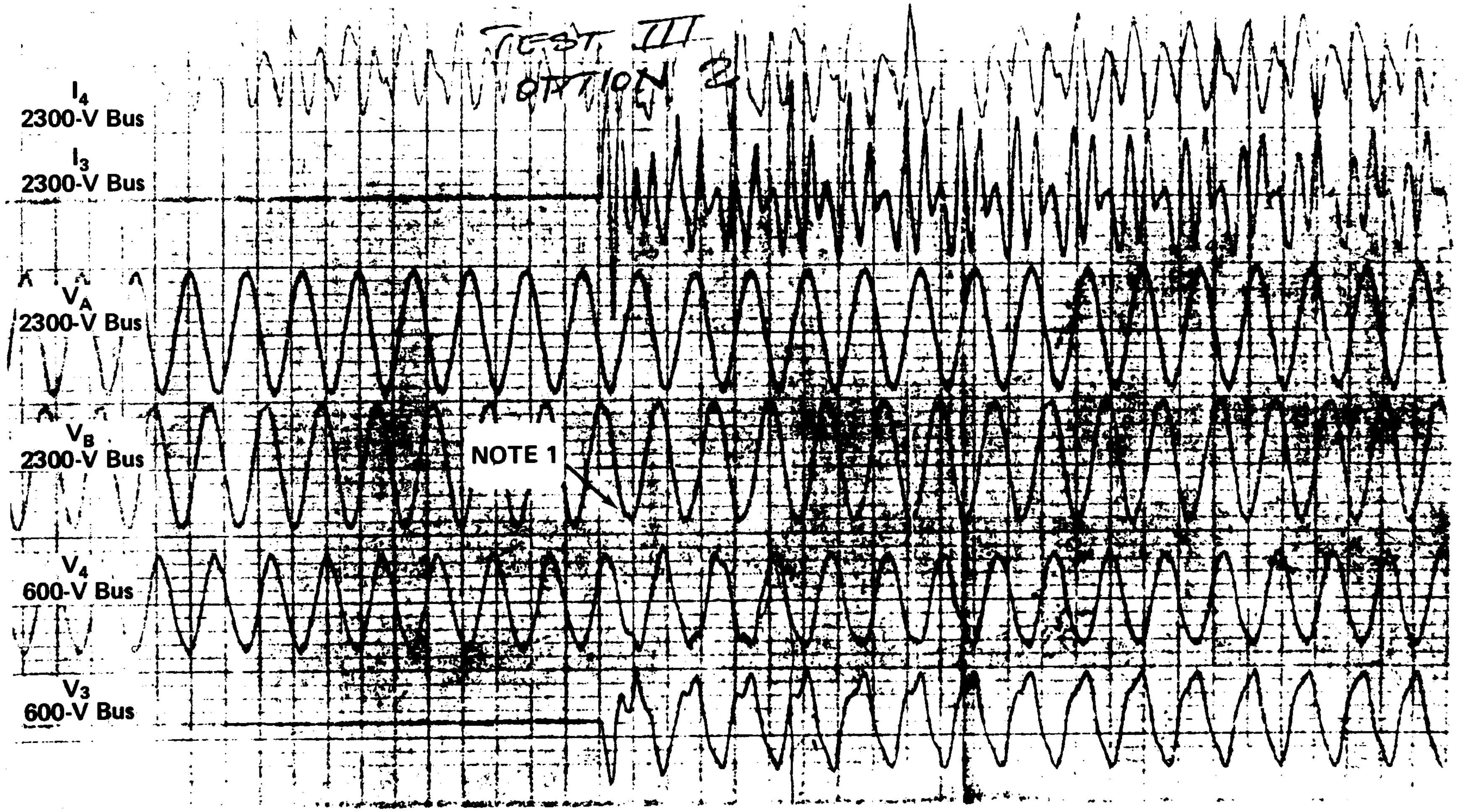
Bureau Conclusions and Comments

We will not make any recommendations or conclusions for two reasons:

1. It is not our place to do so.

2. There are too many unknowns pertaining to the data presented and how it was obtained.

However, we will comment that it appears that the silicon-controlled rectifier drives misfire, causing phase-to-phase faults more often than not when starting other units. This may be due to: (1) transients that are directly or indirectly coupled through the interconnecting circuits, (2) ferroresonance, (3) tuned circuit oscillations induced during switching, (4) improperly placed power factor correction capacitors, (5) improperly selected feedback transformers (i.e., the large inrush alone may distort the voltages enough to misfire the silicon-controlled rectifiers, and (6) improper feedback circuit design (i.e., a delta on the filter side may help improve the 600-V system waveform, and eliminate the problem, more than the delta on the line side).



NOTE 1: Note the distortion in the negative peaks only, and the relationship to the I_3 positive peaks.

Figure D-1. – Test 3-2 oscillogram.

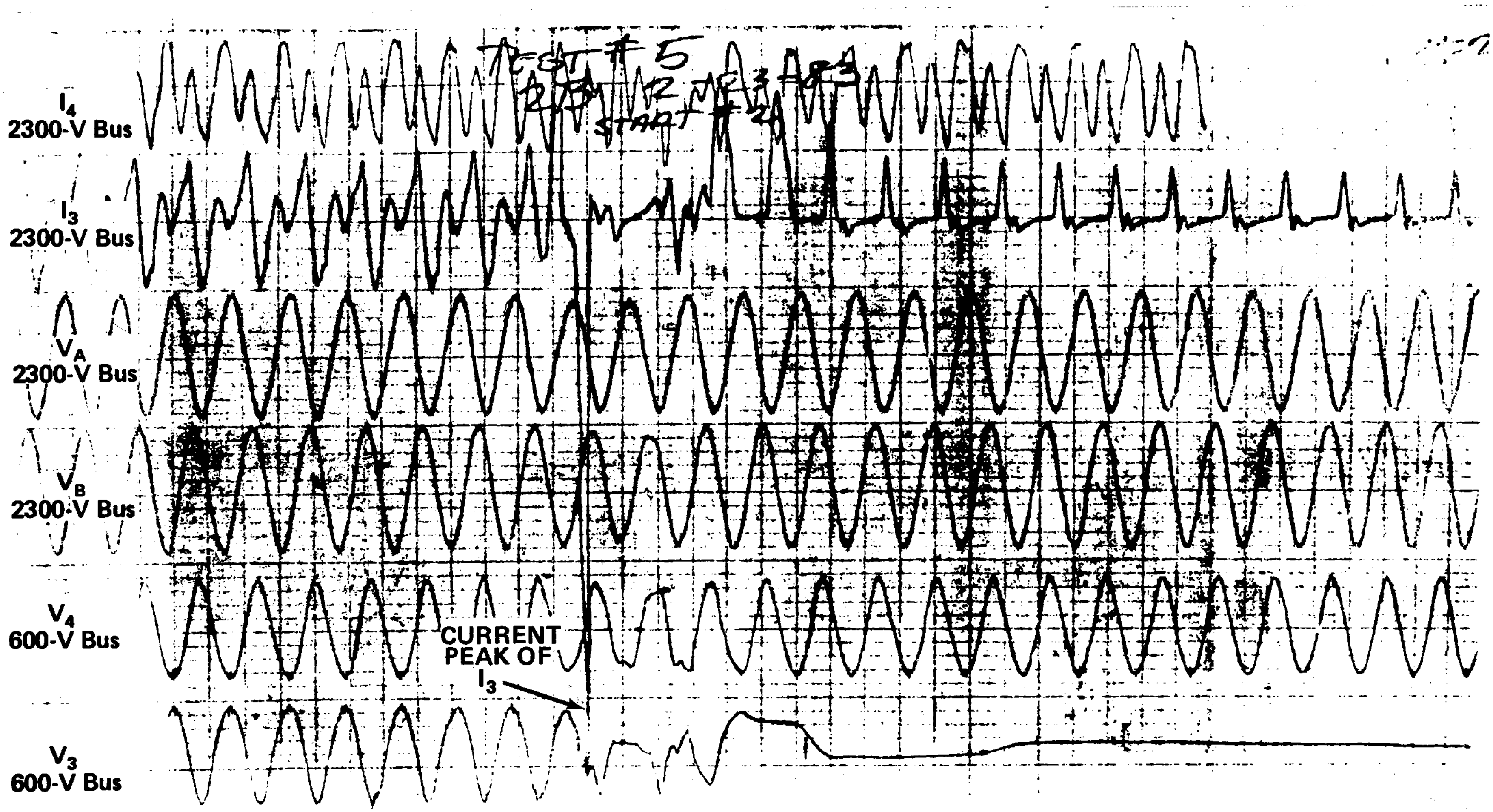


Figure D-2. – Test 5-2B oscillogram.

APPENDIX E

TRANSACTIONS BY IEEE ON POWER APPARATUS AND SYSTEMS

(Courtesy of IEEE)

IEEE Transactions on Power Apparatus and Systems, Vol.PAS-98,No.1 Jan/Feb 1979

NELSON RIVER HVdc BIPOLE-TWO
PART I—SYSTEM ASPECTS

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Abstract: The system aspects of the Nelson River HVdc bipole-two transmission is described. Extension of Manitoba's HVdc system is generally discussed and the paper covers in some detail the system studies, results, criteria, and philosophies related to the planning and overall specification of bipole-two. Frequent reference is made to bipole-one design and experience which effected the bipole-two requirements and design. The information presented is valuable to utility planners considering feasibility and specification of HVdc transmission.

GENERAL ASPECTS OF MANITOBA SYSTEM AND BIPOLE-TWO

Manitoba Electric Power System

Figure 1 shows a map of Manitoba with the major generation and transmission.

Manitoba Hydro's total generating capacity is approximately 2800 MW, including about 2400 MW hydraulic and 370 MW coal-fired thermal. Two additional generating stations (Jenpeg and Long Spruce) on the Nelson River will come into service by 1979 adding about 1000 MW.

The major ac network in the southern and northern systems is 230 kV. Present plans call for significant 500 kV ac lines to be operated in the southern system by about 1990. 230 kV lines parallel the HVdc lines from southern Manitoba to the Nelson River but they are operated in isolation from the dc system at Radisson and Kettle Generating Station. Two Kettle units can be isolated onto the ac system instead of feeding the dc system.

With the hydraulic based system, significant diversity and energy interchange benefits are obtained from ties with neighbouring utilities. There are four 230 kV and one 115 kV ties with east and west neighbour utilities in Canada, and two 230 kV ties with United States utilities to the south. An additional 500 kV ac U.S. tie will come into service in 1980 and other EHV ties are being investigated.

Nelson River Hydroelectric System

The total hydroelectric potential of the upper and lower Nelson River system is about 8000 MW, including 700 MW on the Burntwood River mainly created by diverting the water from the Churchill River into the Nelson. Churchill River diversion was completed in 1977. A Lake Winnipeg regulation structure controls the inflow of water to the Nelson for optimum seasonal water utilization with system load demands.

On the upper Nelson, Kelsey (224 MW) is in operation and Jenpeg (125 MW associated with Lake Winnipeg regulation) will be completed in 1978.

The major generation is on the lower Nelson with five sites — Kettle, Long Spruce, Limestone, Conawapa, and Gillam Island — for a total of about 5500 MW. Kettle (1272 MW) is in operation and Long Spruce (980 MW) will be completed in 1979. The lower Nelson power, at least, will be transmited to southern Manitoba by HVdc transmission.

Figure 2 shows graphically the lower Nelson River development.

Nelson River HVdc Transmission System

Bipole-one, with mercury arc valves, is now complete and previously operated with two dc transmission lines connected in parallel to reduce losses. The sending end rectifier capacity is 1667 MW ($\pm$463 kV, 1800 A), with an overload capability of 1833 MW (1980 A). The $\pm$450 kV rating commmonly referred to is the nominal mid-point line voltage. Voltage control will be compounded for maximum voltage at the rectifier and all ratings of bipoles-one and two will be referred to the sending end for clarity of comparison to the Nelson generation.

F 78 746-0. A paper recommended and approved by the IEEE Transmission and Distribution Committee of the IEEE Power Engineering Society for presentation at the IEEE PES Summer Meeting, Los Angeles, CA, July 16-21, 1978. Manuscript submitted February 9, 1978; made available for printing May 8, 1978.

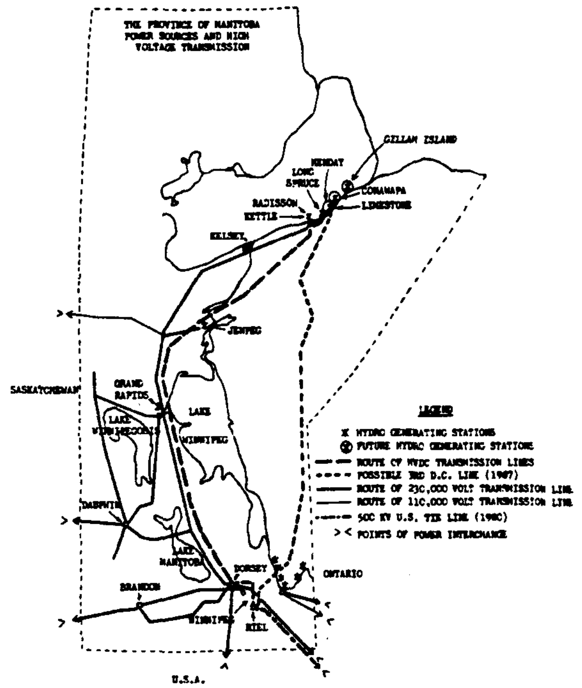

Fig. 1 The Province of Manitoba power sources and high voltage transmission.

Bipole-one can transmit all of Kettle power and part of Long Spruce. The remainder of Long Spruce power will be carried by the first 1000 MW stage of bipole-two scheduled for in-service in the fall of 1978. The second 1000 MW of bipole-two will carry Limestone power and present plans call for a third 2000 MW bipole to accommodate Conawapa and Gillam Island.

Bipoles-one and two are terminated in the south at Dorsey Station, approximately 32 km northwest of Winnipeg. Present plans are to terminate bipole-three at Riel Station roughly 16 km southeast of Winnipeg, to give optimum load flow and transmission requirements in the receiving ac system and to minimize the power concentration at one site for system security. Also, for system security, the third dc transmission line may be located east of Lake Winnipeg roughly as shown in Figure 1.

It was originally envisaged that all rectifier stations would be located at Radisson with 138 kV ac collector lines to all the river plants. This scheme required a large number of ac lines, contained high losses, and minimized the use of reactive power from the generating units. There were also problems of rectifier station voltage control, var supply, and machine self-excitation. Location of bipole-two, and possibly bipole-three, at Henday near Limestone Generating Station solved the above problems and also, along with a change to a 230 kV collector voltage, led to a development with significant capital savings. It was concluded that these savings would easily outweigh any increase in maintenance and operating costs due to separated station sites.

The original transmission concept included plans to have all the bipole rectifiers electrically isolated from each other and to have specific generating stations connected to each bipole. This has now been changed in favour of a 'closed' collector system whereby all the stations are synchronously tied. This gives a more economic and flexible system involving optimum river operation, reactive power supply, and power maintainability for collector line and dc system outages. Studies have indicated that the collector lines and the 230/138 kV autotransformers at Radisson provide sufficient electrical isolation to prevent complete dc system collapse during rectifier end ac system faults. The sending system is designed to maintain full generation capability with any one ac collector line out of service.

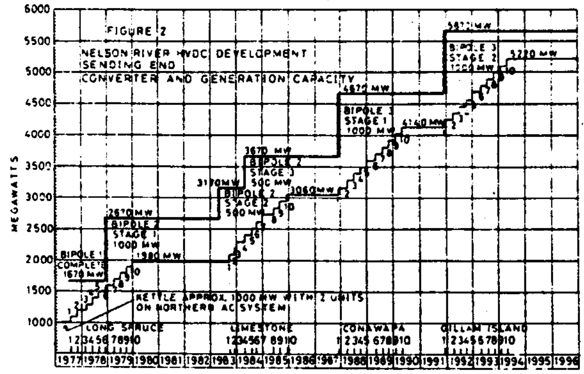

Fig. 2 Nelson River HVdc development — sending end converter and generation capacity.

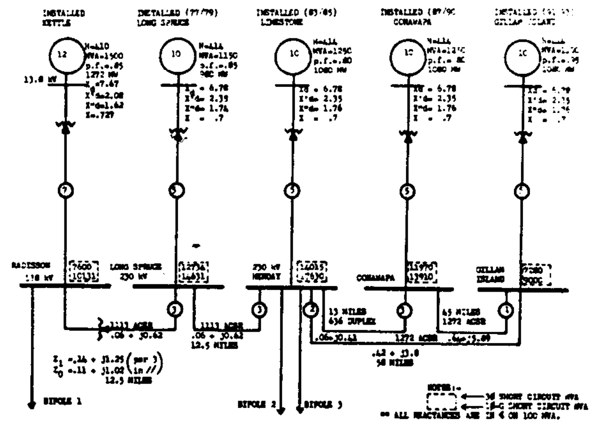

Fig. 3 Nelson River 230 kV ac collector system.

Figure 3 shows the collector system single-line-diagram as well as other system data.

Future studies for bipole-three, and perhaps other stations, will include the possibility of departing again from the present collector concept in favour of an integrated dc station-generating station complex and with unit machine-converter connections.

General Bipole-Two System Aspects and Specifications

Since the early planning stages of the Nelson River, the power capacity of the lower Nelson has been generally revised upwards, leading to a higher rating requirement for bipoles-two and three as compared to bipole-one.

For bipole-one, the system design and installation schedule was determined on the basis of a valve group spare dc capability. For bipole-two, system reliability studies were conducted using dc outage rates based on estimated values and based on statistics and experience from bipole-one. These studies indicated that pole rather than valve group outages were the limiting factor in meeting the loss-of-load criterion for reliability, and that valve group ratings could be increased to 500 MW. It was decided that the system design and schedule for bipole-two should include spare dc capability equivalent to roughly one pole with two bipoles.

The pole reserve concept and allowance for increases in the Nelson River capacity dictated a bipole-two rating of 2000 MW in the winter during peak load. Further increases in river capacity will again reduce the dc reserve to about one valve group in the ultimate development as depicted in Figure 2.

As long as roughly one pole spare is maintained it was recommended that no major equipment spares be purchased. There are no existing, or plans for, spare converter transformers on bipoles-one and two. If increasing generation capacity infringes upon the pole reserve, spare smoothing reactors may have to be considered for bipole-two. Bipole-one can operate with one out of two reactors out of service.

The maximum dc line voltage was set at ± 500 kV at the rectifier. The maximum bipole-two rating of 2000 MW (2000 A) was specified for 'low ambient' or 'winter' ambient temperatures below 26°C dry bulb or 14.5°C wet bulb. The nominal continuous rating of 1800 MW was also specified for ambient temperatures up to 40°C dry bulb or 26°C wet bulb. This means that for most days of the year, even in summer, bipole-two will have a 2000 MW capability. In the system, maximum rating will normally only be utilized during valve group or pole outage conditions and for full generation connected.

The bipole-one and dc line designs, and the bipole-two specification allowed for the possibility of parallel operation of two bipoles on a common dc line. The dc lines can carry up to 3600 A. Parallel operation can be utilized in the event of a dc line or tower loss. Provision is made for the possible paralleling of bipoles-one and two and for bipoles-two and three.

The present concept of paralleling will utilize high speed switches which will not be required to break or commutate significant dc current. Following a line fault, the faulted pole can be isolated from its line and the converters paralleled onto the healthy bipole line without shutting down the healthy pole. Paralleling will be initiated either manually by an operator or automatically by dc line protection and it will be accomplished by an automatic sequence at high speed. Paralleling will only be required when the poles are capable of operating at full voltage, that is with no valve groups out; otherwise, at reduced voltage there is insignificant benefit to paralleling. During parallel operation bipole-two voltage will be reduced to that of bipole-one and transformer tap ranges were specified accordingly.

Deparalleling will also be accomplished by an automatic sequence following initiation by an operator or by fault protection.

It was specified that dc line fault clearing followed by automatic paralleling should be accomplished in approximately 700 ms including one line restart attempt and a fault deionization time of 150 ms. The present cautious attitude toward parallel operation, however, is that automatic initiation will not be used in attempts to maintain system stability for faults but rather will be initiated under carefully controlled conditions and used to continue total power generation during severe outages such as dc tower failures.

System Features Of The Bipole-Two Single-Line-Diagram

Figure 4 shows the dc line switching schematic for three bipoles and for parallel operation. For bipoles-one and two a total of sixteen high speed line switches are required. With these switches each pole converter can be connected to either dc line pole for that polarity.

For parallel operation of bipoles-two and three it was originally accepted that a dc line would be built between Dorsey Station and Riel Station. The cost of this line was estimated at $12 million and it could only be used during parallel operation. Due to the unproven nature of the paralleling concept and depending on when this technique allows full operation confidence, this high capital cost was difficult to justify. A tentative scheme has therefore been conceived whereby one of the planned 500 kV ac lines between Dorsey and Riel could be released for emergency operation at ± 500 kV dc on the outside phase conductors.

Since most radio interference in dc is caused by the positive pole, these poles are located on the inside of the adjacent dc lines for bipoles-one and two.

With the high speed paralleling switches providing alternative pole connections, there is no reason why each dc tower line must be assigned to one bi-pole. For normal operation of bipoles-one and two each dc tower line will hold a positive pole of one bipole and a negative pole of the other bipole. This scheme has been termed 'bipole crossover connection' for convenience. Since the two bipoles have different voltage and power ratings this scheme reduces the maximum possible power loss for a tower failure, reduces switching surge overvoltages for dc line faults, results in a more balanced sending end load rejection for line faults, and allows maximum master power control correction for tower faults without inter-bipole power order transfer.

Figure 5 is a simplified single line diagram of bipoles-one and two.

The Long Spruce SF6 230 kV Station is the first gas insulated station in Manitoba at that voltage level. The only other SF6 station in the system is a small station at 110 kV.

The basic ac switching arrangement at the converter stations is of double bus design with four breakers per 'bay' in a breaker-and-a-third scheme. The corresponding 'element' positions apply to all the elements associated with bipole-two. At Dorsey, bus breakers are located between the bipole-one and bipole-two portions of the station. The ac filters and station service transformers for bipole-one at Dorsey are connected off the main busses and rely on bus breakers for switching. This practice was not continued for bipole-two to increase reliability and ease of operation. Also, as opposed to the tertiary connection in bipole-one, the synchronous condensers or static compensators associated with bipole-two will have their own 'element' positions.

The ac breakers for bipole-two at Henday and Dorsey are minimum-oil type. The bipole-one associated breakers are all air-blast.

Bipole-two is made up of four 12-pulse valve groups each rated 250 kV dc and 2000 A maximum (1800 A nominal) to give 500 MW maximum (450 MW nominal) per group. Bipole-two has a separate electrode line at Dorsey, approximately 22 km long, but connects to the existing ground electrode for bipole-one. This electrode has been extended for 4000 A operation capability for about one month, corresponding to coincident monopolar operation of bipoles-one and two. The Henday electrode is located approximately 11 km southwest of the station.

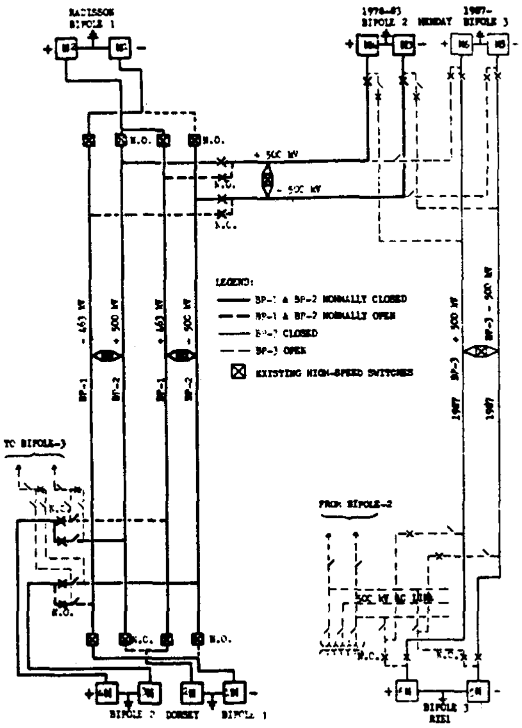

Fig. 4 Preliminary dc line switching schematic for three bipoles and for parallel operation.

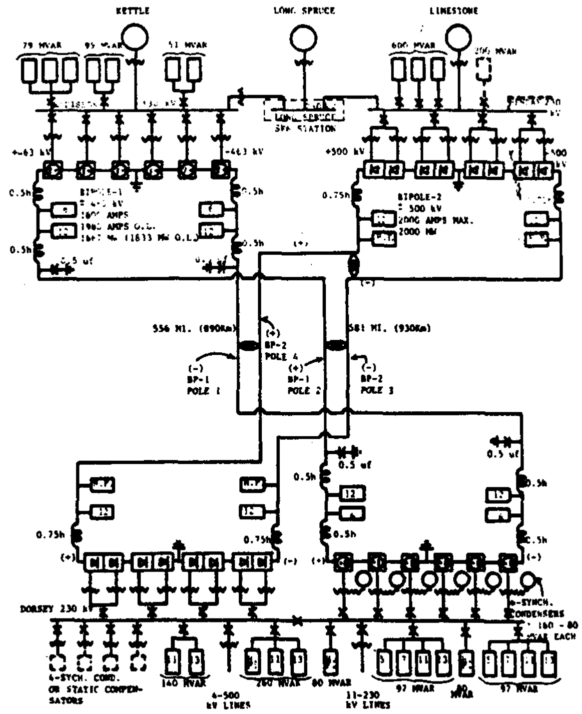

Fig. 5 Basic bipole-one and bipole-two Nelson River transmission system.

Present plans allow for 11-230 kV and 4-500 kV ac lines out of Dorsey station.

Bipoles-one and two will be basically operated in constant power control through their individual master power controllers. Each bipole rectifier controls current and the inverter controls the dc line voltage compounded to the rectifier end. The converter stations can be controlled remotely from the Load Dispatch Office in Winnipeg. Fast telecommunications are required mainly for the ac system damping controls and for fast fault clearing and dc line switching sequences. Some problems have been experienced with the present telecommunications system injecting false current order and power capability signals into the dc. A new PCM (pulse code modulation) microwave link along the present analog (frequency division multiplexing) route on the west side of Lake Winnipeg and a new microwave link to the Nelson River east of the Lake will be installed with bipole-two. Eventually, two digital systems (PCM), one on each side of the Lake, will cure the problems and give reliable telecommunications.

SYSTEM STUDIES AND SPECIFICATION ASPECTS OF BIPOLE-TWO

Nelson River dc Line Design and Parameters

The existing two bipolar lines from the Nelson River are paralleled on the same right-of-way at a minimum spacing of 65 m (212 ft). The total right-of-way width is 112 to 127 m (369 to 417 ft).

The tangent suspension towers are of the guyed single mast type with conductor height of 31 m (103 ft) to 37 m (123 ft) for spans of 427 m (1400 ft) to 488 m (1600 ft). The pole spacing is 13.4 m (44 ft) and the height of the single groundwire above the conductors is 9.94 m (32.6 ft) giving a shielding protection angle of 35° [4].

The conductors are duplex bundle, ACSR 4.06 cm (1.6 in) diameter, 72/7 strand, 1843.2 mcm, with 45.7 cm (18 in) subconductor spacing. The dc resistance per pole (890 km or 556 mi) at 20°C is 14 ohms. The maximum conductor temperature for emergency loading of 3600 A, 40°C ambient, and 0.61 m/s (2 ft/s) wind is 123°C [13].

The minimum tower head clearance, as built with 13.4 m pole spacing, is 3.5 m (138 in) giving a wet CFO of about 1240 kV and a 3-sigma critical withstand (5% standard deviation) of 1050 kV. This clearance is therfore not limiting even for maximum switching surge overvoltages in excess of 1.7 p.u. The 13.4 m pole spacing was based on corona losses.

The suspension insulator strings contain 21 units, each 17.15 x 32 cm (6¾ x 12⅝ in) and 50.8 cm (20 in) creepage distance, for a total insulator string length of 3.6 m (142 in) and a total creepage of 10.7 m (420 in). This gives a specific creep of 2.13 cm/kV (0.84 in/kV) at 500 kV dc. For this insulation the withstand under contamination is the limiting criterion rather than switching surge overvoltages. The insulators were specified for a 2-sigma critical withstand (5% standard deviation) of about 500 kV dc when tested with cement contamination in a fog chamber. This was felt to be acceptable for such a severe contamination since it was found from tests conducted at BPA that worst Manitoba natural contamination was less than 85% as severe as the cement contamination.

The lines were designed for a minimum ground clearance of 8.84 m (29 ft) corresponding to the condition of maximum sag at a maximum conductor temperature of 123°C. The clearance was established from a vehicle clearance of 4.57 m plus an electrical clearance (insulator string length) of 3.66 m plus a 0.61 m margin of safety. The normal clearance will be about 12.2m (40 ft). The minimum clearance more than met all the electric safety codes and standards that were interpreted or existed at that time for dc.

From the test program carried out at the National Research Council [5] the fair weather corona losses are estimated at less that 6.2 kW/km (10 kW/mi) at 500 kV dc. The foul weather (rain) corona losses are about 14.3 kW/km (23 kW/mi).

The average fair weather radio interference was estimated at 44 dB (1 MHz) above 1 μV/m, or 160 μV/m, at 30.5 m (100 ft) from positive conductor at 500 kV. RI levels of from 35 to 50 dB are generally quoted for EHV ac lines and therefore 44 dB should give good RI performance on dc especially when considering that foul weather RI is much lower for dc than for ac and that lower signal-to-noise ratios may be tolerated from dc than ac lines. It was suggested that 10 dB should be subtracted from the dc RI to obtain the equivalent nuisance value to ac.

The calculated maximum conductor surface gradient at 500 kV is 27.7 kV/cm and the maximum gradient at ground level is 10 kV/m slightly outboard (2.44 m or 8 ft) of outside phase conductor and with 12.2 m (40 ft) ground clearance.

dc Side Filtering

Harmonics not filtered are injected into the dc line and by electromagnetic induction, noise is generated in adjacent telecommunication wires, normally telephone circuits using the voice frequency range of 100 Hz to 5 kHz.

Transverse or metallic voltage causes the noise which is heard by the subscribers. In both shielded and open wire circuits, the harmonic currents in the dc line, inducing a telephone circuit noise voltage by magnetic coupling, largely determines the degree of filtering required.

For the average open-wire communication pair, the imbalance has been estimated at 50 dB [7]. The balance of a line is expressed as the difference between the noise-ground and the noise-metallic. Therefore,

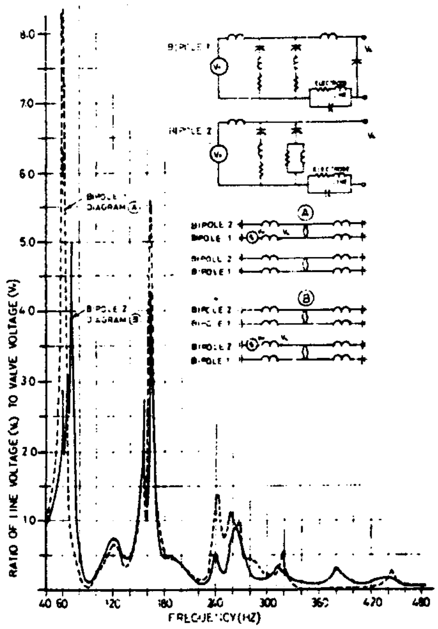

Fig. 6 Frequency response of HVdc lines and terminations.

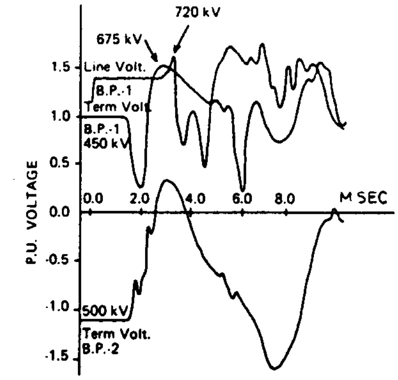

Fig. 7 Typical calculated line voltage due to midpoint ground fault on adjacent pole (line fault at the midpoint on the negative pole of bipole-two with bipole crossover connection and with distributed parameter, lossless line representation).

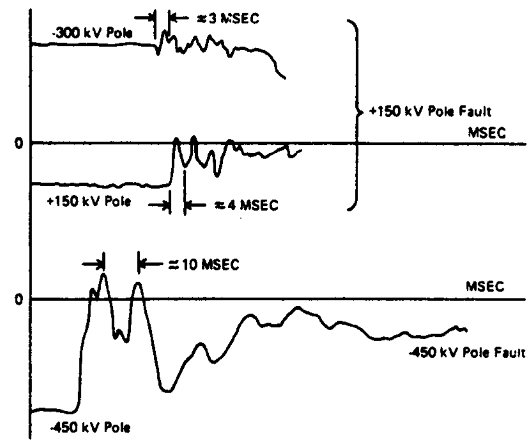

Fig. 8 Bipole-one Hathogram results of two line faults.

dBrnc (noise-metallic) = dBrnc (noise-ground) - 50. The term dBrnc is the induced noise above zero dB level in conventional North American telephone circuits with reference power level for noise of 10^{-12} watts, terminal impedance of 600 ohms resistive, and with the standardized "C message" weighting factor applied for the relative response of the human ear and telephone system equipment for different frequencies. This factor is a maximum at about 1000 Hz.

A considerable number of computer programs were developed to determine the telephone circuit noise and dc filtering requirements in bipoles-one and two. The main programs are:

(1) A program to calculate the harmonic generation from the dc converters.
(2) Program representing multiconductor transmission lines to calculate harmonic voltage and current profiles along the lines using Carson's earth return equations (J. Goodman's IVAC program).
(3) Induced noise program (Stumph's program) determines the noise to ground from the induced harmonic voltages with input data of harmonic current, earth resistivity, and separation.
(4) A number of sub-programs were developed to give a reasonably accurate representation of the multiconductor system in (2) above and provide a good method for bipole-two filter specification or to judge relative filter alternatives before noise was actually calculated to refine the filter performance.

The communication circuit longitudinal interference is largely dependent on the separation, length of parallel run of the exposure, and on total current unbalance (zero sequence) or effective ground current in the power circuit conductors.

In a balanced 12-pulse bipolar system as in bipole-two, the effective harmonic ground current is normally zero and the only noise induction is due to pole separation of the dc conductors. In a balanced 6-pulse bipolar system as in bipole-one, where 12-pulse is achieved only by connection across poles, there will normally exist an effective harmonic ground current for the unique 6-pulse harmonics. Bipole-two will therefore exhibit superior noise performance because of the virtual zero noise generation in normal bipolar operation assuming pole current unbalances due to component tolerances are small. The filtering requirement is then determined by the abnormal monopolar earth return operating mode where an effective harmonic ground current exists.

The Manitoba Telephone System (M.T.S.) and Canadian National Telecommunications (C.N.T.) have indicated their target levels for a desired maximum interference level from the dc system (or any one source) under normal, steady-state operation as follows [7]:

M.T.S.: 17 dBrnc noise-metallic (67 dBrnc noise-ground)
C.N.T.: 26 dBrnc noise-metallic (76 dBrnc noise-ground)

Along the south 240 km of dc line route, there are about 300 M.T.S. rural subscriber lines (varying from 1 to 30 km in length) which use voice frequency transmission. These circuits are open wire, buried cable, and partially buried cable but all were considered as open wire for noise. Each line has an average of six subscribers. About 200 of these circuits come within a 6.5 km corridor on each side of the dc lines.

At the north end for about 320 km, a number of open wire C.N.T. lines exist, one of which is a train dispatchers circuit with voice frequency transmission.

With bipole-one in 3-valve group operation (-300, +150 kV, 640 MW) noise levels on the average M.T.S. circuit were measured from a pessimistic sample of 44 circuits. DC harmonic driving voltages were also measured from which could be calculated the dc line currents and theoretical noise levels. Noise due to dc side harmonics increased an average of 12 dB and noise due to ac side harmonics increased 6 dB. There has been a noticable shift in the ac side noise from lower order harmonics 5, 7, 11, 13th to the 23rd and 25th harmonics. About 73% of the circuits measured were found to be below the target level of 67 dBrnc. About 80% of the calculated theoretical noise levels were within ± 20 dB of measured values. For the final 6-valve group stage of bipole-one, noise levels at the 6th and 18th harmonic will increase about 6 dB and 12th and 24th harmonics will decrease about 10 dB from the measured levels. The measured levels in the average circuit were 48, 49, 60 and 52 dBrnc noise-ground for the 6, 12, 18, and 24th harmonic, respectively.

A general filter requirement was defined as that required to achieve an approximate noise level limit of 80 dBrnc on the worst telephone exposure, or about 80 ± 5 dBrnc to allow for the inaccuracies in the prediction of noise. This filtering would also meet the general target noise level on the average circuit and M.T.S. would in most instances be able to bring the worst exposures down to acceptable levels if necessary.

The bipole-two filters were specified on the basis of dc line harmonic current limits derived from the above system noise measurements. The permitted line currents were based on the average noise in the M.T.S. circuit and the residual line current or ground current that causes that noise, taking into account the noise from bipoles-one and two. The noise level should be less than 67 dBrnc at any one frequency.

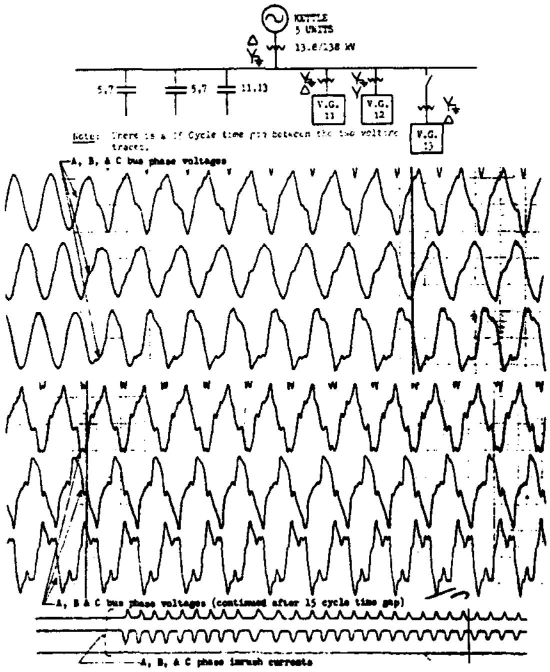

Fig. 9 Field test of transformer currents and bus voltages at Radisson for converter transformer energizing.

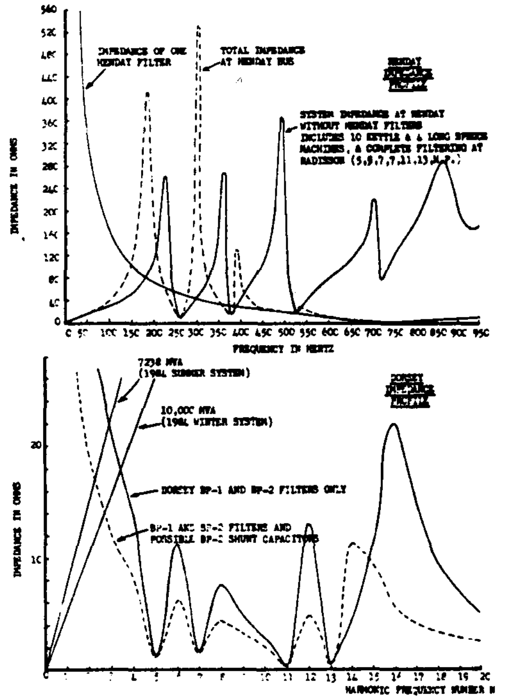

Fig. 10 Positive sequence impedance curves of Henday and Dorsey Systems.

The current limits and noise levels for the 6th and 18th were based on six valve groups in bipole-one plus an assumed pessimistic unbalance in bipole-two. The 12th and 24th limits were based on five (or three) valve groups in bipole-one and one (or three) 12-pulse group monopolar in bipole-two. For two group monopolar in bipole-two the noise would be about 6 dB more but this configuration was assumed to be too pessimistic for infrequent occurances. The expected noise in the average circuit from bipole-one and from bipole-two one valve group is 64, 62, 71, and 60 dBrnc noise-ground for the 6, 12, 18, and 24th harmonic, respectively. Noise from bipole-one 18th is the largest component of the two bipoles, and this is also during normal bipole operation.

The chosen filter is a fixed-tuned (no adjustment for seasonal variation) 12th arm and a high pass arm tuned to the 24th. No M.T.S. circuit relocation is expected to be necessary due to noise from bipole-two.

Bipole-one operated for three days during a 6th and 12th filter outage and no complaints were received. The measured noise on a bad circuit was 62, 94, 76, and 74 dBrnc noise-ground for the 6, 12, 18 and 24th harmonic respectively. The corresponding calculated noise was 83 and 94 dBrnc for the 6 and 12th harmonic. It is anticipated that bipole-two filter outages can also be tolerated for short periods. The highest calculated noise for any filter outage and any harmonic is 96 dBrnc noise-ground in the average circuit.

dc Line Resonance

Selection of dc filters was also based on dc line resonance and dc line overvoltages.

In bipole-one Manitoba Hydro added an additional line side smoothing reactor (from 0.5 to 1 henry) to give improved dc filtering. With one dc line only connected, however, this gave rise to a circuit resonant close to the 60 Hz fundamental which has caused some problems in operation. Special care was taken to avoid bad resonance conditions in bipole-two, even though resonance excitation sources should be less in thyristor converters.

If the dc line is relatively long, the line will have a natural resonance at a low frequency dependent on length and termination. At frequencies below about 360 Hz, the smoothing reactors are the most significant terminal impedance so that the line appears to be almost open circuited and results in a circuit with very little damping. The smoothing reactor size and dc filters should be chosen to avoid a resonance at 60 Hz or a harmonic. The smoothing reactor in bipole-two is 0.75 H.

Bipole-one is resonant at 45 Hz when connected to two dc lines in parallel, and is resonant at 60 Hz when connected to one line. Bipole-two is resonant at 70 and about 160 Hz as shown in Figure 6.

dc Line Overvoltages

For the selected dc filters and smoothing reactors in bipole-two the dc line overvoltages were studied using BPA's Electromagnetic Transients Program [12].

For two identical lines (and terminations) in parallel a line to earth fault at the midpoint of the dc line produces the largest transient overvoltage on the adjacent pole. With the bipole tower crossover connection, however, the two poles on the same tower have neither the *same terminations or the same length. The bipole-two line is* approximately 40 km longer than bipole-one. The minimum advantage of bipole crossover connection for line overvoltages is at least the ratio of the operating voltages of the two bipoles, that is, 463/500 or 0.93. This would reduce a maximum 500 kV line overvoltage, for example, from 1.7 p.u. to about 1.58 p.u. which could save some line flashovers.

From studies done to date with midpoint faults, distributed line *parameters, lossless lines, and bipole crossover connection the maximum* line overvoltages obtained were about 733 kV (1.58 p.u. on 463 kV) on bipole-one and 750 kV (1.5 p.u. on 500 kV) on bipole-two. The corresponding maximum terminal overvoltages were 688 kV (1.48 p.u.) on bipole-one and 730 kV (1.46 p.u.) on bipole-two.

For equal lines, the maximum line overvoltages occur when wave reflections from the terminals add at the midpoint and when the midpoint is also the location of the fault discontinuity. For bipole crossover connection, one would intuitively expect the maximum overvoltages to occur 20 km from the line midpoints when the faults are also located at these points. Further transient studies are necessary to determfine more completely the extent of the crossover connection advantage.

Figure 7 shows a typical calculated line voltage due to a midpoint ground fault on an adjacent pole. The midpoint line voltage on bipole-one shows an initial overvoltage up to about 3 ms due to the zero sequence wave (ground mode). At about 3.2 ms the maximum overvoltage results when the positive sequence wave (metallic mode) arrives back after being reflected from the line terminations. This indicates that the line travel time is about 3 ms.

Figure 8 shows bipole-one hathogram results from two line faults on the existing system. The -450 kV monopolar fault shows the postive sequence wave arriving and then the zero sequence wave, similar to the faulted terminal voltage in Figure 7. For the +150 kV fault the terminal voltage on the -300 kV unfaulted pole has an initial decrease followed by an overvoltage just as in Figure 7. The faulted line terminal voltage is either too small to see the difference between the positive and zero sequence wave or the fault occurred near the inverter terminal and the difference between positive and zero sequence wave arrivals is very small. If we extrapolate the unfaulted pole overvoltage to the case of a full voltage fault the approximate line overvoltage would be 630 kV (1.4 p.u.)

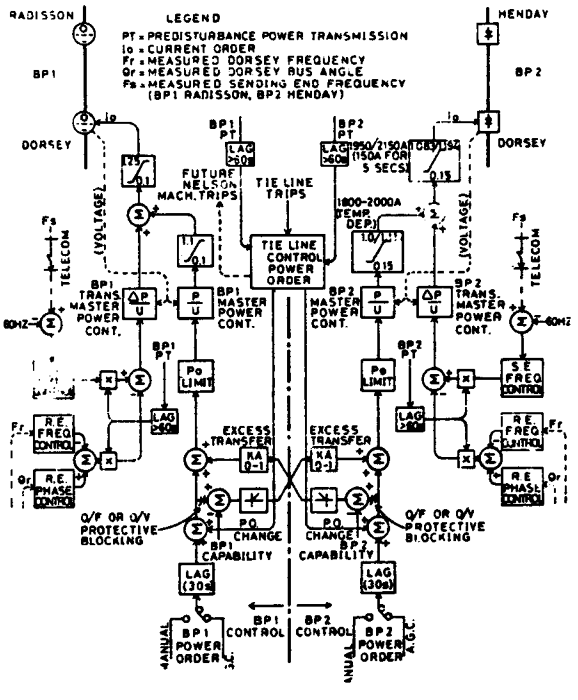

Fig. 11 Functional block diagram of bipole-one and bipole-two power controls.

ac Side Filtering

The degree of filtering required on the ac side of the converter stations to reduce the harmonic currents injected into the connected ac system is generally defined by the three parameters of voltage distortion factor (D), the telephone influence factor (TIF), and the IT factor (IT). The voltage distortion factor D is a measure of distortion of the fundamental bus voltage waveshapes due to all harmonics (normally up to the 50th) and is usually more influenced by lower order harmonics. This factor is most significant for the operation of the power system equipment such as the converters and machines. The telephone interference factor TIF evaluates the voltage waveshape in terms of the inductive influence on exposed telephone circuits. The IT factor is a measure of the interference caused by harmonic currents flowing into the ac system.

The design parameters for the bipole-one filters were an IT of 39 000, a TIF increase of 25, and a D of 4% for Dorsey and a D only of 7% for Radisson.

A number of measurements have been taken on the existing system for an incompleted bipole-one. Noise on M.T.S. circuits has not been a problem at the 5, 7, 11, and 13th harmonics. Calculations extrapolated from the measurements at Dorsey show that the dc link noise level may exceed the design limitations in the ultimate development.

Measurements taken with 3 valve groups (1 x 12, 1 x 6) yielded a voltage TIF of 10, a D of 1.3%, an algebraic total IT for all 230 kV line currents of 34 266, and a vector sum IT of 22 656. An extrapolation of these results to 5 valve groups (2 x 12, 1 x 6) gives an algebraic total IT of 65 961 and a vector sum IT of 43 437, which are above the 39 000 design level. Our computer calculated distortion and IT levels for 5 valve groups (2 x 12, 1 x 6), 100% load, are D = 1.6%, IT = 43 000 (vector); and the levels for 6 valve groups (3 x 12), 100% load, are D = 1.9%, IT = 46 750.

M.T.S. circuit measurements reveal that noise due to ac side harmonics has increased about 6 dB with the 23 and 25th being the most bothersome.

IT meters are presently being installed at Dorsey to monitor the ac line currents and to gain experience on the correlation of IT factor to M.T.S. circuit noise.

The measured D at Radisson with 3 valve groups was 2.1%. The calculated D is 3.2%. There has been no known undue harmonic stresses on the Kettle generators. Tests have been conducted with various filters out of service giving high bus voltage distortion and converter operation remained stable with no known problems. With no 11 and 13th filters connected in one case the distortion was about 15%.

The ac filter measurements and experience on bipole-one has influenced the bipole-two filter design in three ways. Improved filtering at the 23 and 25th harmonics was targeted, the Dorsey filters were made seasonally tuned for improved maintenance and reliability, and the Dorsey filter Q was raised to prevent overloading of bipole-one filters. This is required when bipole-one filters are on tune and bipole-two filters are detuned due to temperature changes in capacitance which forces the current to shift into the bipole-one filters unless the bipole-two filters have a sufficiently low impedance. This low impedance is achieved by lowering the filter resistance.

The bipole-two filter specification of D and TIF was made the same as for bipole-one at both ends and assuming an open circuit system on the ac bus at Dorsey. At Henday, the ac system was defined by all conditions between the minimum and maximum short circuit levels. Previous calculations have shown that the open circuit condition gives roughly the same D and TIF as compared to when the ac system impedance is represented while allowing a harmonic in resonance. Even though the IT level may be the best indication for potential telephone interference, it was not specified because of its complex dependency on the ac system harmonic impedances which vary greatly with frequency and system buildup. Also, resonances can occur at a specific frequency anywhere in the ac system and cause interference even for low IT levels. A reasonable filter is therefore expected from the TIF level specified and future resonance and interference problems can be solved if and when they arise.

A TIF was not specified for Henday because of the isolated ac system and the absence of telephone circuits.

At Dorsey, two 11 and 13th arms and one high pass filter were specified each rated to handle the currents from complete bipole-two at all loads up to nominal rating plus 10% harmonics from the ac system. This scheme gives high filter redundancy for contingencies. In total, there will be two spare 11 and 13th filters, that is, one spare of each associated with each bipole, and one spare high pass arm for the two bipoles. To prevent the distortion and IT from becoming any larger than has been designed for with bipole-one, the bipole-two filters had to be designed to operate in parallel with, share harmonic currents, and not overload the existing bipole-one filters. The high pass filter has been designed to decrease the TIF to about 23 for rated conditions and with all filters connected.

At Henday, three high pass filters were proposed each rated to handle the currents from complete bipole-two at all loads up to nominal rating plus 5% harmonics from the system. This scheme provides ultimate filtering for bipole-two and three with one spare. Space has been allowed in the switchyard to add a possible fourth bank as a second spare in future. These damped filters are tuned to the 12th harmonic to give as low an impedance as possible at the 11 and 13th and also provide filtering for the higher harmonics. The ac system impedance was specified as that corresponding to the short circuit levels of 2500 MVA minimum and 12 500 MVA maximum and with an impedance angle of 83°. The sharing of harmonics with bipole-one filters at Radisson should not be a problem due to the isolation effect of the 138/230 kV transformation.

System Insulation Coordination

The insulation design of all equipment requires an overall system insulation coordination philosophy and specification to establish criteria for insulation levels, margins, arrester applications, system damping, switching operations, system expansion, reliability, and type, magnitude and duration of overvoltages.

For a dc link the insulation coordination can be considered in two parts — the ac side and the dc side.

(a) ac Side: Over an approximate one year period in 1975 at Dorsey, one arrester counter reading was recorded on 66 arresters on the ac side and 3 readings were recorded on 10 arresters on the dc side. At Radisson, 35 readings were recorded on 60 arresters on the ac side and none on 10 dc side arresters.

The presence of large energy storing capacitors in the ac filters imposes abnormal energy discharge and rate of rise of current at sparkover duties on the ac side arresters protecting the converter transformers, filters, and other equipment on the ac bus. To handle these duties special arresters with modified characteristics are applied to bipoles-one and two.

Manitoba Hydro normally installs 192 kV rated arresters on the 230 kV system to protect a traditionally specified 900 kV BIL. The modified arresters have specially designed valve blocks for the discharge duty but retain about 44% protective margin for 900 kV BIL and 20% for 750 kV BIL assuming worst case impulse discharge of 20 kA. The maximum switching surge sparkover is about 2.2 p.u. A 750 kV BIL was not implemented because of the departure from previous practice and because of the fear of possible reliability reduction.

The arresters at Dorsey may be required in future to discharge as much as 1700 Mvar of capacitors in ac filters plus static capacitors. The large capacitance will have the effect of sloping off fast surges and will require high surge energies to charge the system to significant overvoltages. The major consideration therefore on the converter busses are slow switching surges.

At Dorsey transient overvoltages caused by switching and recovery from ac faults are not severe due to the damping of the receiving end system. Peak transients are expected to be considerably below 2 p.u. and highly damped.

The maximum temporary power frequency overvoltage on the Dorsey bus was specified as 1.3 p.u. above nominal caused by a complete block of bipole-one and two (100% dc load rejection) for a maximum of

2900 MW of generation (about 80% of dc capability). These overvoltages were specified as sustained for at least 200 ms before being controlled by eight synchronous condensers (2 out of service). An X'd of 30% maximum for the condensers was used in the digital studies which exclude the effects of transformer saturation and harmonics.

The corresponding maximum power frequency overvoltage on the Henday bus was also specified as 1.3 p.u., sustained for at least 150 ms.

ac filter switching can cause high overvoltages, about 2 p.u., but these are generally single or double spike transients that can be easily handled by the arresters.

Ferroresonant type overvoltages at Henday caused by transformer switching, load rejection and ac system fault clearing have evolved as the most important overvoltages on the ac and dc sides of the stations. They are dealt with under a separate heading below.

(b) dc Side: dc side insulation levels and voltage dimensioning of present thyristor valves are based mainly on switching surge overvoltages, on the surge diverter characteristics for switching surges, and on the design margins.

The maximum temporary power frequency voltages which are seen as the maximum commutating voltages when at least some valves are conducting current (not blocked) provide the basic parameter for the dc side equipment operating and withstand requirements.

The maximum temporary power frequency overvoltage at Henday which leaves valves conducting was specified as 1.2 p.u. above nominal caused by a complete block of one pole on bipole-one plus one pole on bipole-two from overload rating with one pole on bipole-one out of service. This represents an approximate 66% load rejection for connected generators.

The maximum overvoltage at Dorsey which leaves valves conducting was specified as 1.25 p.u. above nominal caused by a complete block of bipole-one from overload rating with one pole on bipole-two out of service (approximate 66% load rejection).

The power frequency overvoltages are caused by the sudden rejection of vars from the converter station when the valves are blocked.

It was specified that for impulse voltages the margin between equipment insulation level and diverter protective level must not be less than 20% and the corresponding margin for switching surge voltages must not be less than 15%.

Switching Overvoltages Caused by Transformer Saturation

Transformer magnetizing inrush currents caused, for example, by transformer energization or by clearing ac system faults, can excite resonances under certain configurations of generators and ac filters in the sending end system. If the system is resonant at the correct harmonic such as the 3rd or 4th then the excitation can produce highly distorted ac overvoltages. These overvoltages are dynamic in that the harmonic voltage peaks superimpose on the 60 Hz wave until they are damped out. The sending end systems at Radisson and Henday are very lightly damped and the resulting long duration overvoltages can be damaging to arresters if they must repeatedly sparkover.

There has been no known insulation problems with respect to these overvoltages in bipole-one but they may account for the higher arrester counter-readings mentioned previously on the ac side at Radisson. The converter transformers have been switched many times. On the other hand, these overvoltages have caused operating problems and restrictions. Transformer switching is not recommended with certain machine-filter combinations and they must be switched at their lowest tap position to effectively raise the saturation knee and reduce inrush. These restrictions increase operating duty on the filter breakers and on the tap changers. On a number of occasions transformer switching has resulted in dc system shutdowns when the long duration ac voltage distortion excited a 4th and 8th harmonic resonance between the dc filter and smoothing reactor causing the dc filter protection to block the dc.

In order to eliminate operating restrictions, minimize ac voltage distortion problems, and decrease the probability of obtaining dangerous overvoltages at least for converter transformer switching it was decided to install preinsertion resistors on the transformer breakers of bipole-two. Consequently, worst recoveries from ac system faults are the only expected cases where high overvoltages can occur causing multiple arrester sparkovers.

Preinsertion resistors decrease magnetizing inrush currents by effectively removing the voltage from the transformer nonlinear inductance when it reaches the saturation or low inductance region of the saturation characteristic and effectively applying the voltage within the linear or high inductance region. It is only necessary for this resistor to be in service long enough to prevent the initial deep saturation. Once this is passed operation within the linear portion of the saturation curve is virtually assured.

Overvoltage studies were conducted on the IREQ dc simulator to determine the effect of and correct parameters for the resistors. With preinsertion resistors inrush currents were generally decreased by a factor of about 10 and highest overvoltages were decreased from about 2 p.u. to about 1.2 p.u. A preinsertion resistance of between 800 and 2000 ohms with an insertion time of 8 to 12 ms was specified. Energy dissipation in the resistor reduces as the size increases. A resistor of 1600 ohms with an insertion time of 5-15 ms (variable) was finally chosen in conjunction with the breaker manufacturer.

A number of tests have been conducted at Radisson for switching filters and converter transformers. The maximum overvoltage for switching a 5, 7, 11th filter was 1.7 p.u. but these contain only one or two peaks. The maximum recorded overvoltages for energizing transformers is about 1.5 p.u. but the overvoltages and distortion were of very long duration (more than 1 second). The overvoltage magnitudes are lower and the duration longer than shown in previous simulation studies which give maximum overvoltages in excess of 2 p.u. The system tests however have always been with random switching and with no point-on-wave control. The long duration prompted a closer investigation of the sending end system damping and best information indicated that the 60 Hz impedance angle was closer to 88° rather than 83° as had been specified. This also was a factor in the decision to install preinsertion resistors at Henday.

In contrast, system tests with filter switching at Dorsey indicates that the receiving end system impedance angle is about 72° normally and should always be less than 80° even with long EHV ac tie lines to neighbouring utilities. Significent overvoltages do not appear at the receiving end like at the rectifier.

Figure 9 shows a typical voltage trace at Radisson for a converter transformer energization.

Two possible explanations for the mechanism of generating overvoltages from inrush have been forwarded in the literature [8], [9], [10]. One explanation uses the concept of the transformer saturation inductance being switched in and out repeatedly; the other explanation assumes the transformer is a current generator driving the filter-system with the inrush current 'train'.

The longer duration overvoltages in the field as compared to simulator tests is normally explained by the higher damping given by the excessive resistance in the model transormer windings and other components. The lower damping in actual system, however, should increase the probability and magnitude of overvoltages according to theory. The fact that recorded overvoltages are generally less than for simulator studies perhaps indicates one or all of the following:

(1) That all worst conditions of resonance tuning, remanence, and switching time has never been recorded on a system.

(2) That frequency dependent damping is not correct on a simulator where perhaps the dc component damping should be much less and higher frequency component damping should be more.

(3) That the recorded overvoltages have generally been on system configurations giving a resonance at the 4th harmonic and not the 3rd. According to theory [10] the maximum is then about 1.5 p.u. as opposed to 2 p.u. for 3rd resonance.

Figure 10 contains positive sequence impedance curves of the Henday and Dorsey Systems showing some possible resonances between the filters and the ac systems.

In general there would appear to be scope for further investigation into these very important overvoltages.

Reactive Compensation and Short Circuit Levels

For most dc projects to date the suppliers have been required to propose and design their own compensation package as part of an overall dc equipment contract and which they interpret as being the best solution for both the dc link and ac system. For bipole-two it was felt that the tendered proposals could have wide variations involving synchronous condensers, static compensators, filters, and static capacitor combinations. The many combinations could create an almost impossible situation for tender evaluation and system var coordination. It was decided to remove virtually all the degree of freedom in the compensation-filter specification and allow only the supply of ac filters. Various filter designs were allowed but the maximum var supply from the filters was specified as 600 Mvar at Henday and 400 Mvar at Dorsey to be switched in blocks no larger than 200 Mvar.

Under the present concept compensation will be installed as required for total dc power on two bipoles in accordance with new Nelson River generation development. With spare capability built into the dc system the compensation is related only to the total Nelson generation and not to total dc capability. This applies to both reactive power and short circuit requirements.

At Dorsey, bipole-one has six synchronous condensers rated 160 Mvar overexcited and 80 Mvar underexcited. Reactive power associated with bipole-one is 960 Mvar from the condensers and 356 Mvar from the ac filters for a total of 1316 Mvar. Initial studies indicated that four additional synchronous condensers plus an additional 400 Mvar from bipole-two filters would be required for full power generation on two bipoles while allowing for two synchronous condensers plus the largest filter bank out of service.

The compensation above with 10 synchronous condensers is considered perhaps unnecessarily conservative because: (a) the outages are pessimistic to maintain full generation; (b) dynamic overvoltages, continuous voltage regulation and var balance can be continuously limited or maintained for zero to full dc loading without generally requiring filter switching; (c) the minimum short circuit ratio (assuming X"d) would be about 4 for the worst system conditions and outages, and would be about 4.4 normally for full generation.

The conservative design above, cost of synchronous condensers, and cost of maintenance has forced a re-evaluation of the Dorsey compensation. Static compensators have emerged as a possible alternative with economic benefits and preliminary simulator studies indicate that bipole-two compensation can be entirely achieved with static compensators (equivalent to 4 synchronous condensers) and with acceptable overvoltage control and dc fault recovery response.

Frequent claims are made today that a short circuit ratio (system short circuit capability/dc power) of 2 or less is acceptable for dc operation for control stability and for recovery from ac system faults and commutation failures. Actually, the acceptable ratio is dependent somewhat on the damping with a higher damped system more favourable. Also, the system-filter-capacitor resonance should be above the 2nd harmonic to avoid excessive ac voltage distortions during fault recoveries. There remains some reservations on the minimum accepted ratio and accordingly it was specified that a minimum ratio of 2.5 would be maintained even for the most pessimistic system and outage conditions. Operation with a smaller ratio should be acceptable during unusual outage conditions. Bipole-one has operated at relatively low powers with no synchronous condensers for significant periods and two bipole operation was successful on the dc simulator with ratios less than 2.

No additional synchronous condensers or static compensators are added for the first 1000 MW stage of bipole-two. Only the 400 Mvar of ac filtering is installed. The short circuit ratio until the second 1000 MW stage comes in with Limestone generation will be a minimum of 2.6 with two synchronous condensers out and with a pessimistic system. The normal ratio will be about 4. In order to provide dynamic voltage or voltage regulation control in lieu of control provided by additional condensers, overvoltage sensitive or logic supervised filter switching will be employed. Also, coordinated condenser-filter operating combinations will be recommended for various dc loadings to maintain proper var balance. The existing condensers will still give fine continuous var control for load variation equivalent to about one bipole, and larger variations will require filter switching. Dynamic power frequency voltages are not expected to rise above 1.4 p.u. on the ac bus for pessimistic conditions and for coincident rejection of both bipoles, before being controlled by the condensers and filter switching. This is acceptable for arresters and other system equipment.

Further studies are yet to be carried out for the condenser/static compensator requirements for the second 1000 MW stage of bipole-two or for the Limestone generation in the mid 1980's.

dc simulator studies conducted at IREQ indicated that commutation failures would not occur for switching filters or capacitors less than about 600 Mvar. This was not a ruling condition and a maximum switched bank of 200 Mvar was specified to give reasonable var control blocks and voltage regulation (about 4% maximum at Dorsey). It was subsequently decided to combine the 11, 13, and H.P. arms at Dorsey for a total of 260 Mvar and switch this filter as infrequently as possible. The second 11 and 13th arms are 140 Mvar, making the total of 400 Mvar.

At Henday, 600 Mvar of compensation is made up of three 200 Mvar switched H.P. filters. The remaining reactive power will be supplied by the generating machines. Space has been provided to install an optional fourth filter or static capacitor if necessary when bipole-three is built. The generators are designed with a power factor based on 2 of 4 Henday filters and one Radisson filter out of service and for maximum loading on bipoles-two and three. The subtransient reactance of the machines are chosen to keep the short circuit ratio above 3 and to limit the power frequency temporary overvoltages for dc load rejections.

Overall dc System Controls

Figure 11 depicts in block form the overall dc system controls.

The previous bipole-one master power controller utilized a Nelson River generation total capability signal which required counting the machines connected to the dc system. Failure of the ac capability signal on several occasions caused control malfunctions and severe system disturbances. Experience has shown that larger frequency excursions at the Nelson River can be tolerated than had been anticipated. Also, with more plants and collector lines, determination of the ac capability signal would become very complex. For these reasons, to prevent northern system overloading and to provide frequency damping, a frequency dependent power order controller for bipoles-one and two replaces the previous capability based system.

Each bipole will have sending end frequency control to make the dc link load frequency sensitive and help stabilize the sending system. Also, receiving end frequency and damping controls will be provided to stabilize the Manitoba system for disturbances and to dampen tie line oscillations. The gains of these controllers will be scaled according to the bipole predisturbance power transmission rather than sending end capability. The sending end controller will restore collector system frequency to about 59 Hz. Further frequency restoration can be obtained by an operator reducing the dc power orders.

Each bipole controller looks at the conditions at its rectifier and inverter stations and controls the bipole power independently of the other bipole. Each bipole will normally respond similarly except if the collector system ties are open so that the rectifiers run asynchronously.

A transfer scheme allows excess power order from one bipole to the other in case of bipole capability loss, as for a pole outage, for example. Restoration of bipole capability allows that bipole to pick up power which was transferred. Bipole current limits prevent overloading of the bipoles regardless of the power orders. For flexibility of operation, the amount transferred can be adjusted from 0 to 100% of the requested transfer. In normal operation the power transfer is zero.

Under power export conditions, when a tie line trips, fast dc power order reduction can greatly assist the southern system stability. A supplementary dc control will be provided which quickly reduces dc power order by an amount proportional to the export load lost. The power order change allocated to each bipole can be in proportion to the power being transmitted on each. For large export trips, for example on the 500 kV U.S. tie, the corresponding dc transfer reduction may cause excessive northern system overspeed and it may be necessary to simultaneously trip some generators.

Under import conditions and for tie line trips the bipole controllers could allow limited (up to 10 MW/machine) power order increases without lowering the sending end frequency excessively (below 59 Hz).

The dc transmission has produced requirement for some important peripheral system controls and protections:

(a) As a backup to main frequency control and if excessive overfrequency occurs, Dorsey overfrequency relaying blocks valve groups on a per valve group basis. Other overfrequency relays also trip synchronous condensers to protect the machines from excessive overspeed in case they become isolated from the system and connected only to a running valve group.

(b) With only one 5, 7th filter connected at Radisson, a 320 Hz (64 x 5) resonance occurs in this filter at 64 Hz if the 11, 13, and H.P. filters are also connected. Therefore, at 64 Hz the 11, 13th filters are tripped to prevent the possibility of the 5, 7th filters tripping on overload. To avoid undesirable filter trips, overfrequencies are limited whenever possible. For example, dc power transfer reductions due to tie line trips can be limited.

(c) Bipole-one is presently blocked at 54 Hz by a Radisson underfrequency relaying to protect the auxiliary supply motor-generator sets from overload. An underfrequency protection at Radisson to block valve groups in stages is presently being considered as a backup to the main sending-end frequency control.

(d) The somewhat unstable combination of the torque-speed characteristics of the hydraulic turbines and the constant power nature of the dc rectifiers requires that the generator governors be designed to maintain stable operation. Normally, if communications are intact, stable operation for most faults and disturbances will be greatly assisted by the sending end frequency control modulating the dc power.

The governors are electro-hydraulic utilizing power (Long Spruce) and gate (Kettle) feedback. Electronic joint load controllers at each plant provide machine load balancing and steady state 60 Hz operation. Machines can be transferred on and off joint control. Failure of the joint load control causes the joint load to trip off and the units are controlled by their own governors on a droop line.

(e) Static exciters with continuously acting voltage regulators are used to provide fast rate of change of field current especially to control voltage and possible self-excitation during dc load rejections. Also, to prevent self-excitation, all Radisson filters are tripped for a total bipole-one block, the last Kettle generator connected is automatically tripped, Kettle units have frequency sensitive overvoltage relays, and operator restrictions prevent bad combinations of valve groups, filters, and machines.

At least 50% generator transformer voltage drop compensation is provided on the regulators to minimize bus voltage variations for load changes.

A joint voltage and var controller at Kettle holds the Radisson bus set-point voltage via a process computer and balances machine vars in steady state. At Long Spruce the joint controller balances machine vars but the steady state var adjustments between plants will be coordinated by operator control of Long Spruce units on joint voltage control. The Limestone controller will hold the Henday bus set-point voltage.

Stability studies have shown that undamped electromechanical power oscillations can occur between the Nelson River plants after disturbances such as sending end ac system faults or bipole blocks. These oscillations have a frequency of from 1 Hz to 2 Hz. Excitation system power stabilizers will be added at all plants to damp out these oscillations.

CONCLUSION

Extension of Manitoba's HVdc system to include bipole-two involved extensive system studies on many aspects. These studies, and the experience with bipole-one, played an important role in defining the requirements and specification of bipole-two.

Acknowledgements

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SUPPRESSION OF TEMPORARY OVERVOLTAGES CAUSED BY TRANSFORMER AND AC FILTER INRUSH CURRENTS AT THE SHIN-SHINANO FREQUENCY CONVERTER STATION

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Abstract - The temporary overvoltage problem associated with ac-dc converter stations is caused by the converter transformer magnetizing inrush currents. A damping resistor type suppressor is introduced into the AC filter of the Shin-Shinano Frequency Converter Station to suppress such overvoltages. A considerable suppression effect is noted during the field testing, and the validity of the overvoltage prediction by calculation is also confirmed by the measurements.

INTRODUCTION

When the ac system connected to a high-voltage dc converter station contains only generators and/or has loads only at the end of long-distance transmission line far away from this station, closing the converter transformer onto the ac system can cause temporary overvoltages or voltage distortions of duration up to about one second due to the converter transformer magnetizing inrush current containing low harmonics and the resonance of the ac filter bank with the ac system [1, 2, 3].

In the Shin-Shinano Frequency Converter Station, Tokyo Electric Power Co., Inc. (hereafter referred to as "Shin-Shinano FC"), it was predicted that when the converter transformers and ac filter bank together are closed onto the 50Hz ac system and depending on the system conditions, considerable overvoltages would appear on the 275kV ac bus and much larger overvoltages across the 5th harmonic filter arm capacitors. For the predicted overvoltage of 3.2 p.u. (per unit value, 1 p. u. = normal or rated voltage) across these capacitors on the 50Hz side, although there are various methods of suppression [1], we adopted a damping resistor type suppressor in the 5th harmonic filter itself [4]. The use of such a suppressor within the ac filter is a unique concept, and there is no preceding example in application. This suppressor consists of a resistor in series with each 5th harmonic filter arm at the neutral side; this resistor is left inserted for one second after the filter is switched in to damp transient oscillations of duration up to about one second, then shorted out by a switch. The selected resistance value for this resistor is 300 ohms.

This paper reports on the suppression effect of this damping resistor type suppressor as observed during the field testing before and after the commencement of Shin-Shinano FC operation, shows the validity of the prediction by analog simulation, and discusses a more detailed digital simulation taking up the result of nearby power station's transformer closing test as an example of overvoltage without the suppressor.

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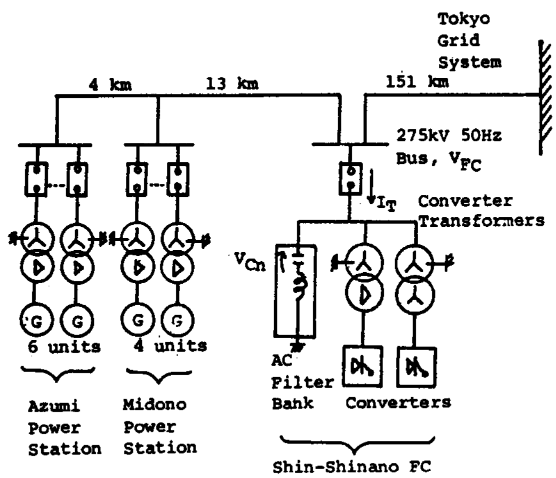
SYSTEM CONDITIONS AND TEMPORARY OVERVOLTAGES

Figure 1 shows an outline diagram of the 50Hz ac system connected to Shin-Shinano FC, which has on this side an ac filter bank (72 MVA capacity at the fundamental frequency) and two converter transformers (187 MVA x 2) and they are integrally connected to the ac system network through a circuit breaker. The ac system includes one Azumi transmission line to the Tokyo grid system (about 150 km in line length) and two nearby pumped storage power stations at Azumi and Midono. The Azumi power station has six generator-motors and the Midono four generator-motors. The system short-circuit capacity varies greatly from 1600 MVA to 4000 MVA depending on the number of nearby generators on line.



V_{FC} = FC ac bus voltage

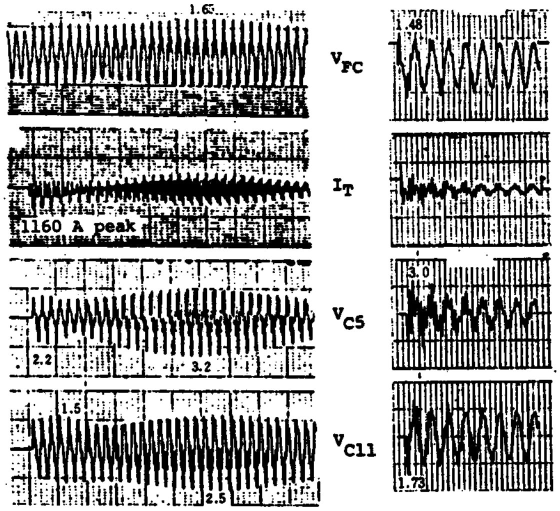
V_{Cn} = Voltage across nth harmonic filter capacitor

I_T = Ac current into FC

Fig. 1. 275kV 50Hz ac system connected to Shin-Shinano Frequency Converter Station.

Figure 2 shows two examples of the overvoltage simulation by analog computer (single-phase calculation): (a) when the connection-point circuit breaker is closed at a zero-crossing of the bus voltage, showing transient oscillations of duration about one second due to the transformer magnetizing inrush currents; (b) when the circuit breaker is closed at a peak of the bus voltage, indicating transient oscillations of short duration less than 0.01 second.

Figure 3 plots the overvoltage simulation results with the number of nearby generators on line on abscissa. The highest overvoltage appears when none of the generators is on line and the connection-point circuit breaker is closed at a zero-crossing; it is 1.65 p.u. on the bus and 3.2 p.u. across the 5th harmonic filter capacitor.



(a) Closing at zero-crossing of bus voltage, no nearby generator on line.

Per unit voltage values shown.

(b) Closing at peak of bus voltage, three Azumi and one Midono generators on line.

Fig. 2. Analog simulation of transient oscillations when converter transformers and ac filter bank are closed onto 50Hz ac system, no suppressor.

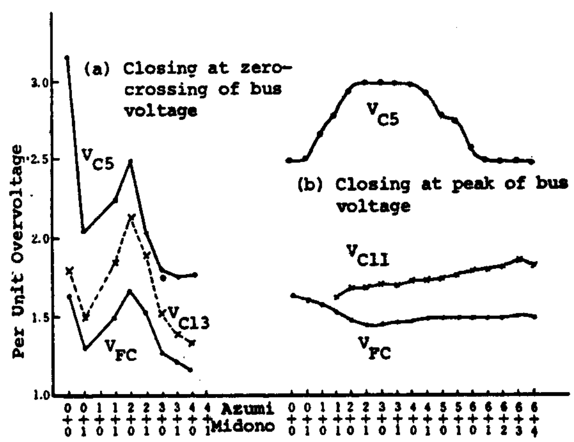


Fig. 3. Predicted per unit overvoltages on bus and across filter capacitors under various system conditions (different numbers of nearby generators on line), no suppressor.

Figure 4 shows a correlation between the positive sequence impedance and the frequency for the ac system and ac filter bank viewed from the bus. By adding the saturation reactance of the converter transformers to the ac system impedance when no nearby generator is on line, we see the resonance of the ac system with the ac filter bank will occur at a frequency very near to the 4th harmonic. This explains why the bus and capacitor voltages of Fig. 2 (a) contain a large amount of the 4th harmonic component. Similarly, it is easy to see from the curves of Fig. 4 that the second highest of these overvoltages shown in Fig. 3 (a) is due to the 6th harmonic component.

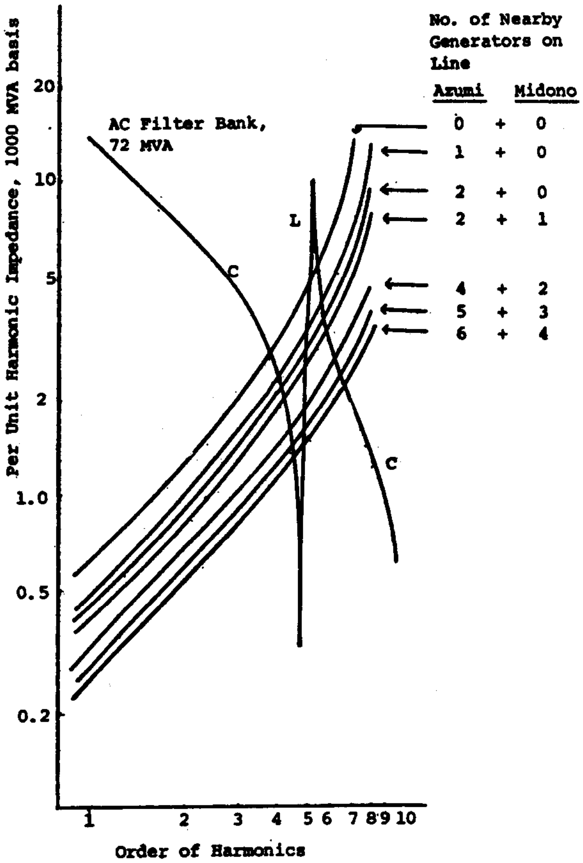


Fig. 4. Per unit harmonic impedance of 50Hz ac system and ac filter bank, viewed from bus of Shin-Shinano FC.

For the 60Hz ac system connected to Shin-Shinano FC, overvoltages of such a long duration are unlikely partly because the system short-circuit capacity is 2000-2500 MVA and partly because there are loads nearby.

DAMPING RESISTOR TYPE OVERVOLTAGE SUPPRESSOR IN AC FILTER

The 5th harmonic filter arm capacitor has to be extremely oversized if designed to the predicted overvoltage of 3.2 p.u. across the capacitor. This necessitates some measures to achieve a reasonable capacitor design and also to reduce overvoltages on the bus.

In order to reduce overvoltages caused by the transformer magnetizing inrush currents at a high-voltage dc converter station, J. P. Bowles suggested the following five methods [1]:

(a) Selecting filter impedance;
(b) Addition of wideband low harmonic (damped) filter;
(c) Use of surge diverters at ac side;
(d) Series resistance insertion at the time of transformer energization;
(e) Timing control of transformer energization.

Method (a) has difficulties in avoiding all the possible resonance frequencies, such as the 2nd, 3rd and 4th harmonics, when the system conditions change from time to time; (b) leads to an increase in costs and losses; (c) requires a high handling capacity of energy under repeated discharges, and (e) is impractical in treatment of the residual fluxes. Method (d) was recently adopted in Nelson River HVdc Bipole-Two Hendy Converter Station [5].

Various overvoltage suppression methods were considered and reviewed for Shin-Shinano FC, and it was finally agreed to have a means of suppression within the ac filter bank, 50Hz side. Since the highest overvoltage was predicted on the 5th harmonic filter, it was then decided to use a damping resistor type of suppressor in this filter, which inserts a resistor in the neutral side of each of the filter arms at the time of connection to the 50Hz ac system. A larger damping resistance gives a greater suppression effect, but our analysis has shown that the suppression effect has a tendency to saturate above 300 ohms. Figure 5 shows a change of the resultant total impedance for the 4th harmonic of the ac system and ac filter bank with the damping resistance, indicating a similar tendency and supporting our analysis. On the other hand the resultant total impedance for the 5th harmonic, also shown in Fig. 5, increases with the damping resistance, causing a larger 5th harmonic voltage component to appear. In view of the above, we selected the damping resistance value of 300 ohms.

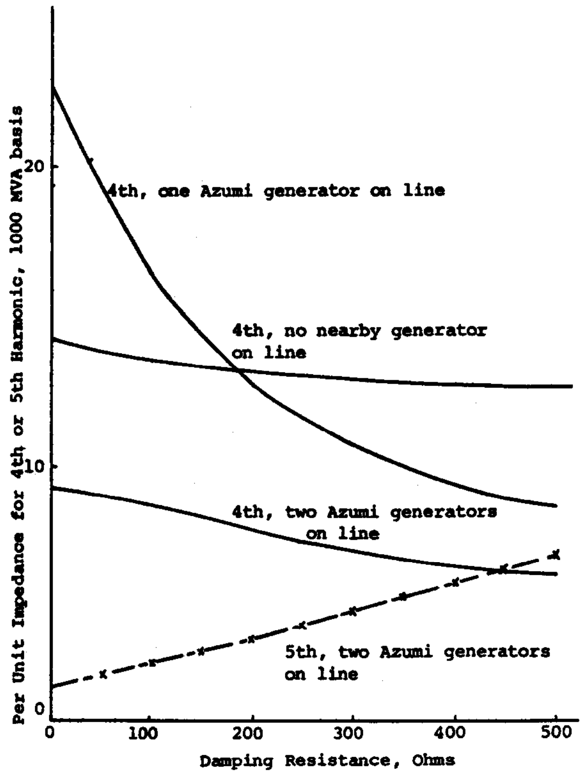


Fig. 5. A change of resultant total impedance for 4th and 5th harmonics of 50Hz ac system and filter bank with damping resistance in 5th harmonic filter, viewed from bus of Shin-Shinano FC.

Figure 6 shows a simplified diagram of the damping resistance insertion circuit: the shorting-out switch S2 for the damping resistor DR (300 ohms) is open when the main circuit breaker S1 between the Shin-Shinano FC 50Hz bus and the converter circuit complex shown is open and remains open for one second after S1 is closed. When S1 is opened, the high-speed overcurrent relay HOC opens S2 and makes it ready for the next closure. Since the utilization factor of the damping resistor is very low, the resistance element is a "cast-grid" type normally used for neutral grounding resistors in power circuits.

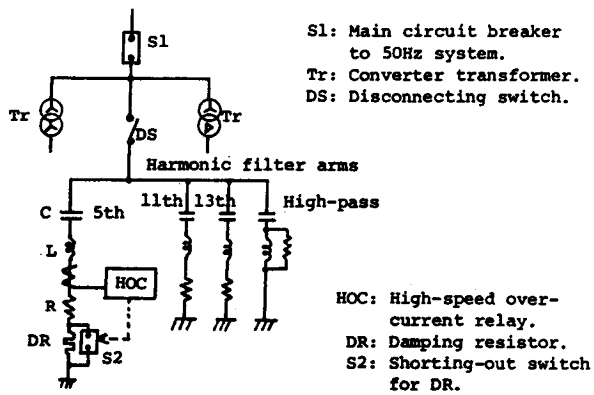


Fig. 6. Damping resistance insertion circuit.

EFFECT OF DAMPING RESISTOR TYPE SUPPRESSOR

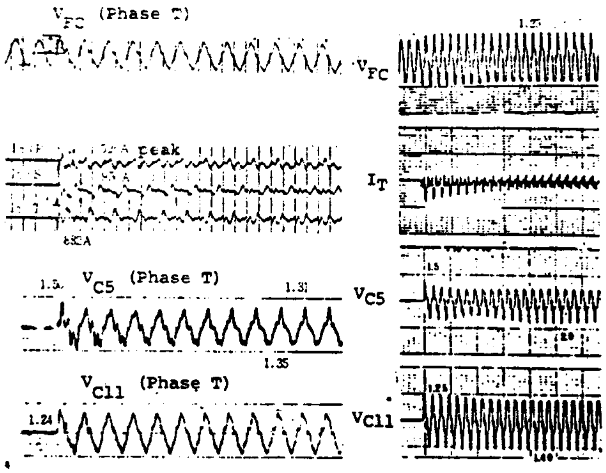
Field Test Results

For three days from the 18th to 20th of June, 1977 a total of 66 closing tests were conducted under five different cases of the system conditions (the number of nearby generators on line varied). Table I lists the highest overvoltage measurements recorded for both 50-Hz and 60Hz ac systems together with the simulation results for comparison. For the 50Hz ac system, 1.6 p.u. overvoltage of short duration (0.01 second or less) measured on the bus, 2.2 p.u. short duration and 1.9 p.u. long duration (about 1 second) measured across the 5th harmonic filter arm capacitor indicate a high suppression effect of this suppressor. Although tests

Table I. Highest per unit overvoltages, predicted and measured.

	On 50Hz System				On 60Hz System			
	Simulation		Measurement		Simulation		Measurement	
	Long	Short	Long	Short	Long	Short	Long	Short
Bus	1.33	1.65	-	1.6	1.54	1.68	-	1.6
V_{C5}	2.0	2.5	1.9	2.2	1.9	2.0	1.7	3.0
V_{C11}	1.4	1.9	1.4	1.7	-	1.9	-	2.1

Note: 1) 300-ohm damping resistor type suppressor is present in 5th harmonic filter on 50Hz system.
2) "Long" means overvoltage of about one-second duration, and "Short" that of duration 0.01 second or less.
3) "-" means no data or not a large value.
4) V_{C5} and V_{C11} are overvoltages across the 5th and 11th harmonic filter arm capacitors, respectively.



(a) Test record 50C-12-1-12 (19 June, 1977).

(b) Analog simulation, single-phase.

Fig. 7. Transient oscillations when suppressor is present, no nearby generator on line.

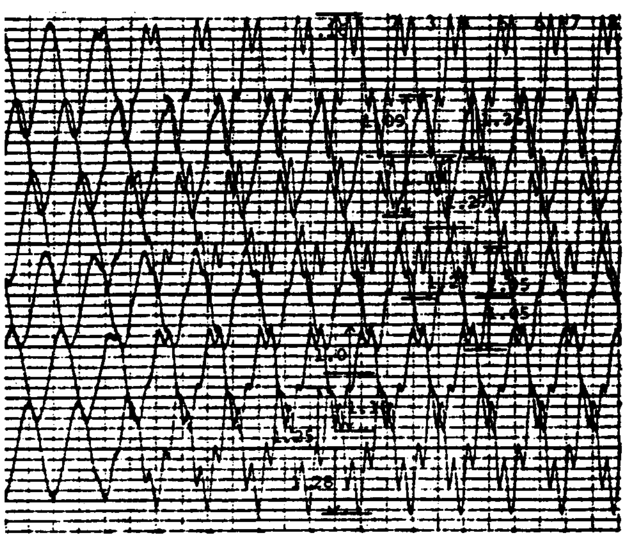
without the suppressor were not conducted, 3.0 p.u. short duration maximum measured across the filter capacitor on the 60Hz ac system will support the above statement. As a supplement to Table I, comparison between the measurements and the simulation (single-phase calculation by analog computer) is also shown in Fig. 7; the predicted values by the simulation are nearly equal to or only slightly higher than the measurements, indicating a high accuracy of the simulation.

Overvoltage Without Suppressor

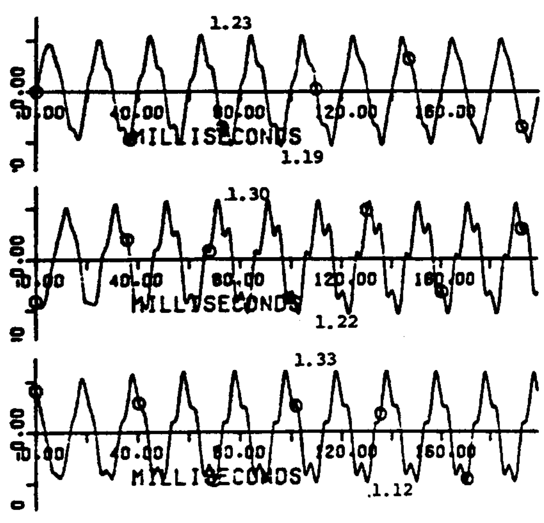
As an example case where no damping resistor is inserted in the 5th harmonic filter, we took up the measurement data from the closing test of No.1 transformer, 130 MVA, Midono Power Station, which was carried out on 25 September, 1978, and compared them with the result of a digital simulation outlined below:

Digital Simulation Method: The single-phase circuit calculation by analog computer, as used for the above overvoltage prediction, simulates the basic circuit structure in a simple form (the transformer saturation characteristic as a nonlinear element, the transmission line as a π equivalent circuit, and each filter arm as it is composed) and is a powerful technique to calculate for a wide variety of cases and to grasp the macro aspects of the phenomena (such as shown in Fig. 3). It is, however, difficult to apply this technique for three-phase circuit simulation. For our simulation, therefore, we used a digital technique with the electromagnetic transient program (EMTP) developed by the BPA [6] through modeling the case that: (i) Midono No.1 transformer be a set of three single-phase, double-winding saturation transformers with star-delta connections and with polygonal line approximation for the core magnetization characteristic; (ii) the transmission line be a distributed constant circuit assuming as if the line wires were transposed, with the frequency dependency neglected but the zero-phase sequence constants around 200 Hz introduced to take the damping into consideration; (iii) the generator voltage behind the subtransient reactance Xd" be constant; (iv) the harmonic filters be as they are composed; (v) the converters under floating operation be neglected, but the converter transformers be taken into consideration including the core magnetization characteristic. The size of the circuit model was 76-node, 96-branch, and it took about 26 seconds for IBM 370/168 computer to calculate five cycles (100 ms).

Midono No.1 Transformer Closing Test: Figure 8 compares the oscillogram recorded during the test with the EMTP simulation result. The oscillogram indicates the highest overvoltage of 1.36 p.u. on the Shin-Shinano FC bus after 7-8 cycles of the transformer closing, whereas the simulation result shows it to be 1.33 p.u. after 4-5 cycles of the closing. There is also a difference in the size of the superposed 4th harmonic component between them. These are considered to be due to the difference in the phase angle and



(a) Oscillogram. From top to bottom: Phase R, S and T voltages, and R-S, S-T and T-R voltages.



(b) EMTP digital simulation. From top to bottom: Phase R, S and T voltages.

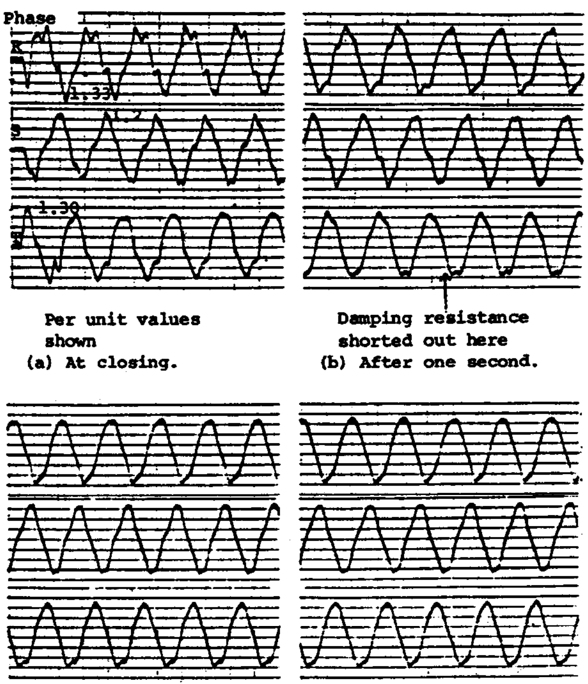
Fig. 8. Transient oscillations appeared on Shin-Shinano FC bus when Midono power station's No. transformer is closed onto line, one Azumi generator on line.

residual flux at the time of No.1 transformer closing, and also due to some circuit constant differences (such as in the short-circuit capacity of the Tokyo grid system) which, even small, can greatly influence the result since this example involves only one Azumi generator on line (see Fig. 4). In the simulation, the transformer was closed at a zero-crossing of the phase R voltage with the resiudal flux assumed nil, and the Tokyo grid system was so modeled that its voltage behind the reactance under the assumed short-circuit capacity be constant.

As an example of overvoltages observed during the field testing at other HVdc projects, it was reported that 1.5 p.u. overvoltage maximum was recorded at the Kingsnorth project and 1.43 p.u. at the Nelson River both after 50-100 cycles of converter transformer energization [2].

Duration of Voltage Distortion

In Shin-Shinano FC, because the suppression is done by the damping resistance in the filter, the overvoltage on the bus normally reaches a maximum in 1 or 2 cycles after its closure onto the ac system. Figure 9 shows portions of a long-time oscillogram recorded at the time when the FC was closed onto the 50Hz ac system; even after one second where the damping resistance is shorted out, some voltage distortion continues. The result of harmonic analysis on this oscillogram by a spectrum analyzer is shown in Fig. 10 to give a clearer picture.



(a) At closing.
(b) After one second.
(c) After four seconds.
(d) After eight seconds.

Fig. 9. Oscillogram of Shin-Shinano FC bus voltages when converter transformers and ac filter bank are closed onto 50Hz ac system, suppressor present and no nearby generator on line.

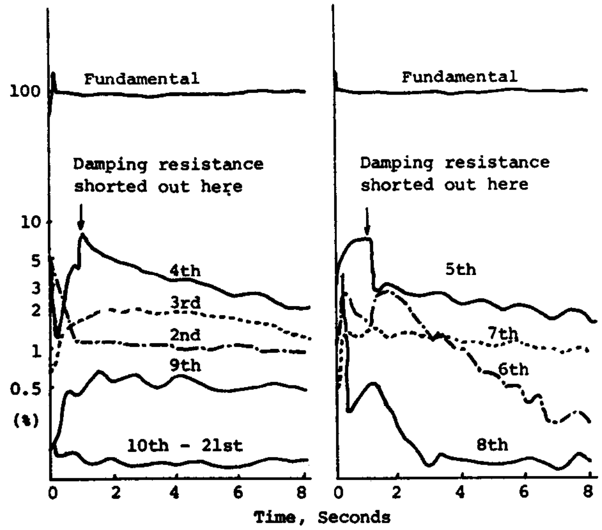


Fig. 10. A change of harmonic voltage amplitudes (% normal fundamental voltage) with time, harmonic analysis result on Fig. 9 voltage waveform.

CONCLUSIONS

The damping resistor type suppressor in the ac filter at Shin-Shinano FC has been found, during the field testing, to have a large overvoltage suppression effect. Without this suppressor, overvoltages caused by the converter transformer magnetizing inrush currents would lead to unwanted operation of the overvoltage protective relays on the ac bus and necessitate much oversized capacitors in the 5th harmonic filter.

We believe that this new concept of including a damping resistor type suppressor in the ac filter will offer a simple, yet powerful solution to the temporary overvoltage problem associated with the transformer magnetizing inrush currents at HVdc converter stations.

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Discussion

T. Subbarao (General Electric Company, Pittsfield, MA): The authors have described a method to reduce overvoltages caused by the interaction of transformer inrush currents with the resonant circuit of ac system and filters at an HVDC station. In addition to the methods mentioned by the authors in page 2 of the paper, to suppress overvoltages, one more method can be considered which will be a variation to the insertion of damper resistor. This consists of switching in the filter branch (or branches) *after* closing the transformer breaker. For example, the 5th harmonic filter can be switched in, one second after the transformer energization. Absence of the 5th harmonic filter moves the resonant frequency away from the frequency of the inrush current, which will be usually near the 3rd or 4th harmonic.

A similar overvoltage situation arises when the frequency of the ac generators connected to the dc system varies. This is described along with the filter switching phenomena to alleviate the resonance problems, in Ref. A.

A ground fault on the ac side of the converter transformer momentarily depresses the ac voltage, which will rise again after clearing of the fault. This will also cause magnetizing inrush currents to flow in the transformer. Do the authors consider that the over current relay will operate fast enough to insert the damper resistor in the circuit for this situation?

A method which perhaps was not available when the Shin-Shinano station was built is now possible because of the zinc oxide resistor. This makes it possible to build surge arresters for high energy absorption. The surge arresters when connected across filter reactor will conduct during the inrush period and suppress overvoltages across the filter and ac bus. This will have the following advantages over the use of damper resistor:

1. More reliable in operation because there will be no switching and interlocking involved.
2. Will be available during faults in the ac system.
3. Smooth operation with no reinsertion transients.

Have the authors evaluated the energy dissipation required by a surge arrester for an equivalent level of overvoltage suppression as that described in the paper?

REFERENCE

[A] T. Subbarao and J. Reeve "Dynamic Analysis of AC Harmonics in High Voltage AC/DC Power Systems During Frequency Excursions" Paper No. C75186-2 presented at 1975 IEEE - PES Winter Meeting

Manuscript received August 7, 1980.

T. Sakurai, K. Murotani, and K. Oonishi: We appreciate Mr. T.Subbarao's discussion to our paper. We agree with the discusser that it is useful for avoiding the switching overvoltages to switch in the filter branches after closing the transformer braker. However in this case there must be one additional 275kV circuit breaker for the ac filters. In our case the rating of the shorting-out switch S2 of Figure 6 is the 24kV circuit breaker. For the simplicity of the ac switching system, two convertor transformers and the ac filter are integrally connected to the ac system network through the one circuit breaker in the Shin-Shinano FC.

The discusser considers the frequency excursions from 60Hz to 68Hz of the Isolated generation in the Ref. A. The Shin-Shinano FC interlinks between the eastern 50Hz and the western 60Hz ac system in Japan, so there is no possibility of such large frequency excursions.

Fig. 1 shows the 3LG simulation result by EMTP. It was assumed that 3LG fault occurres in the Tokyo grid system at 20msec and are cleared out successively about 40 msec. The overvoltages decrease quickly for about 4 cycles. The magnetizing currents of the convertor transformer are nearly peak when the fault currents are cleared, and the sustained overvoltages such as those mentioned in the paper don't occur.

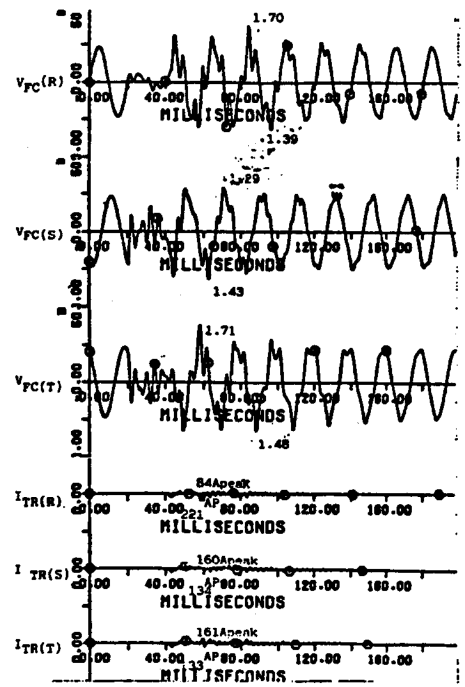


Fig. 1 3LD digital simulation(EMTP)

We agree with the discusser that zinc oxide arresters across filter reactors are useful for insulation coordination in the filter elements. But zinc oxide arrester for an equivalent level of overvoltage suppression such as that described in the paper will be difficult, because the rated voltage across filter reactor is high due to the rated harmonic currents.

Manuscript received September 3, 1980.

APPENDIX F

SYSTEM CAPACITY INVESTIGATION

INTRODUCTION

The Fountain Valley Pumping Plant adjustable-speed, solid-state, motor drives were misoperating and tripping off-line whenever an attempt was made to start a second unit. The prime contractor, the consultant to the prime contractor, and the manufacturer of the motor drives suspected system impedance to be the cause of the problem. The manufacturer stated that the variable-speed system required a minimum system SCR of 20 to 1 and a maximum voltage variation not to exceed ± 10 percent. Therefore, in early June 1983, system impedance tests were performed at the pumping plant. This evaluation of system strength was required to determine if the plant motor starting problem was in any way related to system impedance. This appendix consists of an analysis and discussion of the system impedance test data obtained on June 6 and 7, 1983.

To obtain the necessary system impedance, it was agreed by all parties involved that the data could be obtained by starting one of the motors as a conventional induction motor (rotor short circuited). It was also agreed that standard current and voltage transducers of 0.1 percent accuracy, connected to a laboratory grade strip chart recorder, would be sufficient for measuring the system parameters. This equipment was augmented with a laboratory grade oscillograph. Although less accurate than the strip chart recorder, the oscillograph could provide valuable information on current and voltage waveform and offset.

The oscillograph and strip chart recorder records of the conventional induction motor start are shown on figures F-1 and F-2. Copies of the oscillograph records have been touched up for purposes of reproduction; however, all data and measurements were obtained from the original oscillograph records.

SHORT CIRCUIT CAPACITY MEASUREMENTS

Data from the strip chart recorder indicate the motor current was 960 A, and the voltage on the 2400-V bus dropped 17 percent from 2410 to 2000 V_{rms}. This results in a calculated 2300-V bus short circuit rating of 23.5 MV·A. The oscillograph records indicate the 13.8-kV system voltage dropped 7.2 percent from 13.82 to 12.82 kV. This results in a calculated system short circuit rating of 55.4 MV·A, which is about 13 percent greater than the 49 MV·A value used by the Bureau during the final design. Details of these calculations are presented on calculation sheet No. 1 at the end of this appendix. Steady-state values were used to calculate the short-circuit, megavolt-ampere capacity of the system. Using initial transient current and voltage values during the first few cycles of the induction motor start resulted in a short circuit capacity 2 percent larger than the steady-state value. This indicates that, with respect to disturbances on the 2400-V bus, the transient system capacity is equal to the steady-state system capacity. This is to be expected at this low voltage level.

VOLTAGE MEASUREMENTS

Prior to running induction motor test 2, a normal start of unit 4 was made to check the instrumentation. After the induction motor starting test, three more tests were performed. The entire test series and descriptions are listed in table F-1.

Table F-2 shows the data obtained from the oscillograph records. Based on this data, the peak inrush current during initial energization of the unit 4 feedback transformer, filter, and motor stator varies from 1046 A to a peak of 1330 A. This resulted in about a 4 percent voltage drop on the 2400-V bus and a 2 percent drop in the 13.8-kV system voltage. Based on a rated motor current of 153 A_{rms}, 216 A (peak), the inrush current varied from 4.8 to 6.2 per unit peak. Energization of the unit 3 stator (feedback transformer and filter were preenergized) resulted in the same voltage drop of 4 percent on the 2400-V bus. The majority of these voltage drops lasted no more than one or two cycles.

The system voltage drop during the induction motor starting test was 17 percent on the 2400-V bus and 7.2 percent on the 13.8-kV bus. Calculations based on rated motor current resulted in system voltage drops of 2.7 percent at the low side of the station main transformer (2400-V bus) and 1.1 percent at the high side of the station main transformer (13.8-kV bus). The 2.7 percent voltage drop was the system voltage drop and should not be misinterpreted as the voltage drop at the motor terminals. The relevant data and calculations are presented on calculation sheet No. 2.

The step change in the strip chart voltage trace before and after a loaded unit tripped off-line provided some indication as to the motor terminal voltage drop produced by a loaded unit. The strip chart records of tests 3 and 5C (table F-3) indicated that a loaded motor reduces an average motor terminal voltage drop of about 1.3 percent. The June 16 and 17 tests also resulted in motor terminal voltage drops of about 1.3 to 1.5 percent, according to the DVM (digital voltmeter) measurements.

The motor terminal voltage drops, based on the system capacity measurements and at rated motor current, resulted in calculated voltage drops of about 1.2 percent. The data and calculations are presented on calculation sheet No. 3 at the back of this appendix. The analysis indicates that the system voltage drop is reactive whereas the motor load is mostly resistive. These different impedances result in a large phase angle difference between the two voltages which, through vector analysis, shows why the system voltage drop is not equal to the motor terminal voltage drop.

JUSTIFICATION OF CONVERTER RATING

The justification for rating the converter at 200 kV·A was based on the fact that the feedback transformer must be able to handle both the converter capacity and the var requirements of the filter. At 65 percent speed, the motor load is about 42 percent of rated. At this speed, the converter is required to handle 35 of the 42 percent power; or essentially 15 percent of the motor rating, which is 95 kV·A. The filter var rating was originally specified to be 120 kQ; however, 220 kQ were actually supplied and this has recently been modified to 170 kQ actually installed. This resulted in a total drive circuit capacity of 153 kV·A as conceived; 240 kV·A as supplied, and 195 kV·A as the presently rated capacity. Therefore, 200 kV·A was used as the basis for the drive circuit rating. This is somewhat marginal in that it leaves very little room for transient capacity and various derating conditions. It is now obvious that the 200-kV·A feedback transformers, as originally supplied, were underrated by 40 kV·A.

DRIVE REQUIREMENTS

The drive manufacturer required a power system of sufficient strength so that the drive would not be subjected to voltage fluctuations greater than ±10 percent; and also stated that, on startup, the variable-speed drive would limit motor load current to 1 per unit or less. In addition, the manufacturer informed the Bureau, after the June tests, that in future designs they will specify that the short circuit ratio must be equal to or greater than 20 to 1. This is to ensure proper operation of the variable-speed drive.

SHORT CIRCUIT RATIO

The IEEE Standard 519-1981, "Harmonic Control and Reactive Compensation of Static Power Converts," defines SCR (short circuit ratio) as the system short circuit capacity in megavolt-amperes divided by the converter capacity in megawatts. The SCR is referred to in the IEEE guide primarily for estimating the percent distortion factor. The guide also states that the SCR and percent distortion factor are primarily used to define the effect of the harmonics on the power system voltage waveforms.

Based on the IEEE definition, the SCR at the drive is 16. This value is the same as the per unit short circuit current on the low voltage side of the feedback transformer (on the transformer base) and includes the system impedance as well as the transformer impedance. In calculating the SCR, it was assumed that the drive system capacity was equal to the feedback transformer rating of 200 kV·A. This is to account for the total load on the feedback transformer, which in this case includes the filter capacitors. Failure to account for the filter capacity would result in an error in the SCR calculations. The resultant SCR is rather low and somewhat insufficient both with respect to the manufacturer's stated requirements and the guidelines established in IEEE Standard 519. The SCR on the high side of the feedback transformer is 117, and is also the SCR on the low voltage side of the feedback transformer, if the transformer is assumed to have negligible impedance. The SCR on the drive side of the feedback transformer, assuming the system impedance to be zero (infinite bus), has been calculated to be 18.8. Again, it is important to note that the SCR is insufficient. It now becomes apparent that the feedback transformer specified and supplied by the drive manufacturer was inadequate in meeting the required SCR. Obviously, the SCR has been limited by the rather undersized (high impedance) feedback transformer. Reducing the transformer impedance in half (or doubling the transformer rating) would result in a more than adequate SCR of 28.5. This seems to be logical in that the starting of a second unit is marginal at best. The installation of a double size transformer would cut transformer impedance by 50 percent and thereby reduce the harmonic and/or harmonic like voltages by 50 percent. The induced harmonic voltage appearing across the transformer is approximately the harmonic current times the transformer impedance at the harmonic frequency.

It appears that the drive manufacturer failed to consider the percent harmonic distortion factor requirements in the design of the Fountain Valley drives and associated equipment. It is also our understanding that the system provided is one of the first designs by the drive manufacturer that included a feedback transformer in the system. The drive manufacturer, in one of our earlier discussions relating to the startup problems, stated that other variable-speed drive manufacturers had oversized their feedback transformers for no apparent reason. Based on our analysis of field test data, it is apparent there is indeed a reason why the manufacturer's competitors rate their feedback transformers as they do. The feedback transformers provided for Fountain Valley were obviously undersized. It also appears that the specified SCR of 20 may have been rather conservative and somewhat meaningless with respect to what should have been specified for the equipment.

The IEEE standard indicates the percent distortion factor for a SCR of 16 to 1 ranges from 10 to 18 percent. The variation is the result of the converter direct voltage and the delay angle (alpha). The relevant data and SCR calculations are shown on calculation sheet No. 4 at the back of this appendix.

VOLTAGE AND SYSTEM CAPACITY REQUIREMENTS

Several times, the manufacturer has made reference to the system capacity as being insufficient, and stated that the equipment would not operate properly if subjected to voltage variations greater than ± 10 percent. This voltage specification essentially defines the system capacity requirement. The actual startup problem and the worse case current conditions both occur at the time of initial energization of the feedback transformer, motor stator, and filter. This makes it very basic to prove that the system is of sufficient capacity because the maximum voltage drop at the time of initial energization has been measured to be 4 percent on the 2400-V bus and 2 percent on the system 13.8-kV bus. Obviously, this is well below the 10 percent requirement of the drive manufacturer. Even in the unreasonable case of including the steady-state voltage drop of a running unit while starting a second unit, as the drive manufacturer insists on doing, there is only a 5.5 percent voltage drop. The starting problems encountered in starting a second unit cannot be blamed on a 5.5 percent voltage change, 1.5 percent of which is a steady-state voltage drop which in no way can contribute to the voltage distortions that indirectly result in startup problems. Based on all of these facts, the system capacity and voltage support capability are more than adequate to meet the drive manufacturer's requirements.

CONCLUSIONS

The system capacity is more than adequate to maintain both the 2.4- and 13.8-kV bus voltages. The feedback transformer impedance is higher than it should be according to both the drive manufacturer's requirements and the IEEE standards and, as a result, is the limiting factor in determining the SCR, capacity, and percent distortion factor on the 540-V bus. It appears that the drive manufacturer did not consider the SCR and the resultant percent distortion factor required for proper converter operation when selecting the feedback transformer megavolt-ampere rating and impedance. Obviously, the feedback transformer must be sized to obtain a proper SCR and not just for the converter rating as was done at Fountain Valley. As previously mentioned, the specified SCR of 20 may have been somewhat conservative and meaningless for Fountain Valley.

Table F-1. – Description of tests performed at Fountain Valley Pumping Plant on June 6, 1983.

Test No.	Description
1	Normal start of unit 4.
2	Unit 4 started as a conventional induction motor.
3	Unit 3 started initially, then unit 4 was started, at which time unit 3 tripped off-line. Next, unit 2 was started, at which time unit 4 tripped off-line.
4	Drive circuits of units 3 and 4 were disabled. Unit 3 was started initially, unit 4 was started 5 seconds later.
5	In test series 5, drives were no longer inhibited, and the unit 3 feedback transformer was wired directly to 2400-V bus.
5A, 5B	Tests 5A and 5B were somewhat of a loss due to blown fuses and an overcurrent relay lockout in unit 3.
5C	Unit 4 was on-line running, but tripped off-line when unit 3 was started.
5D	For this and all subsequent tests, the 18-V, a-c potential transformer circuits in the unit 3 drive were monitored on the oscillograph along with most of the signals that were monitored earlier in the test series. On test 5D, unit 4 was on-line running when unit 3 was started, and both units remained on-line. However, unit 4 tripped off-line about 50 seconds after the startup of unit 3 due to an overcurrent relay operation; unit 3 remained on-line.
5E	Unit 4 was on-line running when unit 3 was started. Both units remained on-line, but eventually tripped off-line together. Unit 4 tripped off-line due to an overcurrent relay operation.
5F, 5G, 5H	For these tests, the speed of unit 4 was reduced, thereby decreasing the motor load to prevent the unit from tripping off-line again due to an overcurrent relay operation. Three successful starts of unit 3 were made while unit 4 was running.

Table F-2. – Data from oscillograph records obtained on June 7, 1983.

Test No.	Description	Data
1	Energizing unit 4	Inrush, I ≈ 1100 A (Peak) 2400-V drop, V ≈ 4% 13.8-kV drop, V ≈ 2%
2	See figure F-2	
3	Energizing unit 4, at which time unit 3 tripped off-line	Inrush, I ≈ 1046 A (Peak) 2400-V drop, V ≈ 3% 13.8-kV drop, V ≈ 2%
	Tripping of unit 4 due to bridge short circuit when unit 2 was started	Short circuit, I ≈ 1075 A (Peak) 2400-V drop, V ≈ 1% 13.8-kV drop, V ≈ 0%
4	Energizing of unit 4 after unit 3 had been energized; both drives were disabled.	Inrush, I ≈ 1330 A (Peak) 2400-V drop, V ≈ 4% 13.8-kV drop, V ≈ 2%
5C	Tripping of unit 4 due to bridge short circuit when unit 3 was energized. (No. 3 feedback transformer was tied directly to 2400-V bus)	Short Circuit, I ≈ 1415 A (Peak) 2400-V drop, V ≈ 7% 13.8-kV drop, V ≈ 2% 2400-V drop before and after removal of load and short, V ≈ 1%
5E, 5F, 5G, 5H	Energizing of unit 3 while unit 4 was running. In all tests, unit 3 feedback transformer was tied directly to 2400-V bus.	2400-V drop: V ≈ 4% (5E) V ≈ 4% (5F) V ≈ 3% (5G) V ≈ 4% (5H)

Table F-3. – Strip chart recorder voltage requirements.

Test No.	Description	Data
3	When starting unit 4, unit 3 tripped off-line	Voltage drop = 1.6%
	When starting unit 2, unit 4 tripped off-line	Voltage drop = 1.2%
5C	When starting unit 3, unit 4 tripped off-line	Voltage drop = 1.2%
		Average voltage drop ≈ 1.3%

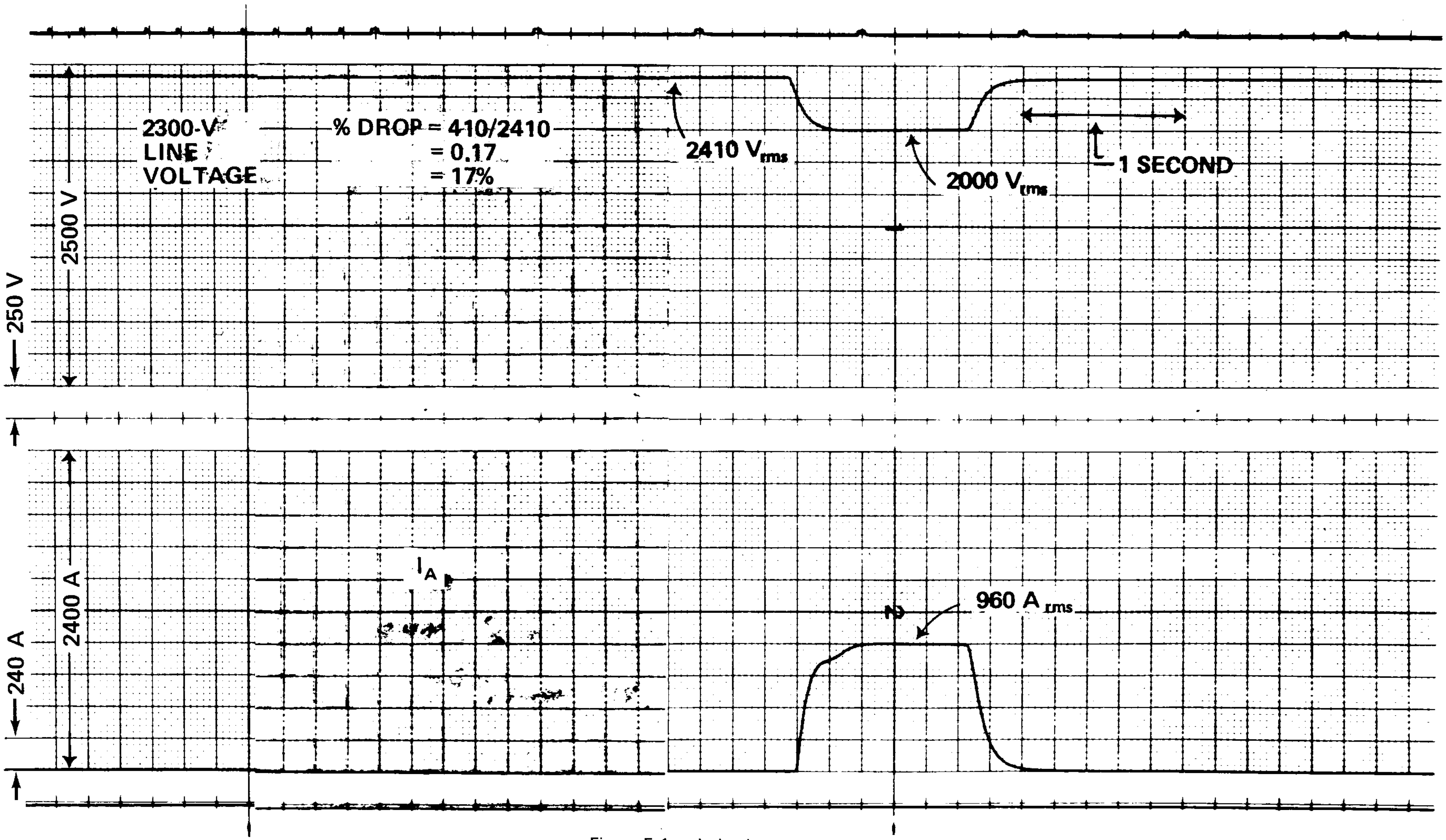


Figure F-1. – Induction motor start.

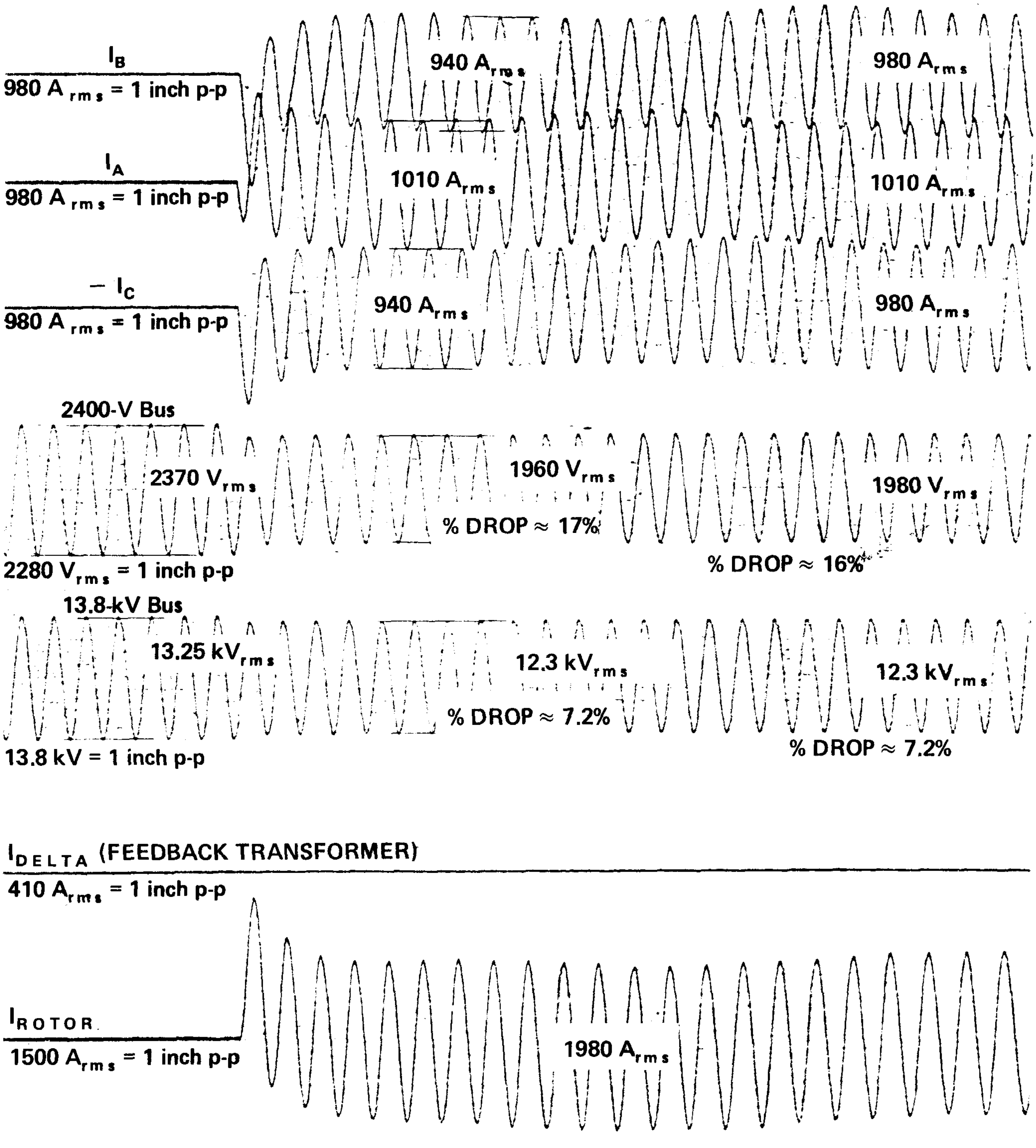


Figure F-2. – Induction motor start oscillogram.

CALCULATION SHEET NO. 1

Induction Motor Start
Test No. 2 - June 6, 1983

Prior to starting: V_{DVM} = 120.70 V (DVM = digital voltmeter)
PT Ratio: 2400:120 or 20:1
Bus V_{DVM} = 120.70 (20) = 2414 V

Data from Strip Chart Recorder:
Prior to test: Bus V = 48.2 div.,(50V)/div. = 2410 V
Note: This is in close agreement with DVM reading.
Starting V = 40 div.,(50 V)/div. = 2000 V
Drop V = (2410 - 2000)/2410 = 17.0%
Starting I = 20 div.,(48A)/div. = 960 A
Fault megavolt-amperes $= (kV)(I)(\sqrt{3})(10^{-3})$
$= (2400)(960)(\sqrt{3})(10^{-3})$
$= 3.991$ MV·A for 17.0% drop
Therefore, the short circuit megavolt-amperes on the 2400-V bus is:

$$3.991/0.170 = 23.5 \text{ MV·A}$$

From oscillograph records:
Voltage drop for 13.8-kV system = 7.2%
Therefore, the system's short circuit megavolt-ampere rating is:

$$3.991/0.072 = 55.4 \text{ MV·A}$$

CALCULATION SHEET NO. 2

System Voltage Drop

The calculations shown on this sheet were based on the data from the Induction Motor Start Test.

The induction motor was rated at 700 horsepower, 153 amperes, and 95.2 percent efficiency at about a 90 percent power factor.

The induction motor test resulted in a 17 percent drop on the 2400-volt bus at 960 amperes and a corresponding system voltage drop of 7.2 percent on the 13.8-kilovolt bus.
Therefore,

System voltage drop (including station transformer) at rated motor load is:

$$(0.17)(153/960) = 2.7\%$$

System voltage drop (excluding station transformer) at rated motor load is:

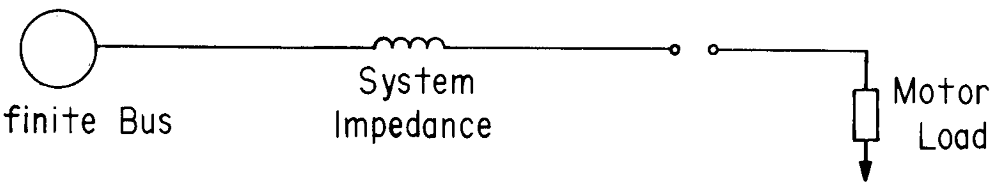
$$(0.072)(153/960) = 1.1\%$$

CALCULATION SHEET NO. 3

Motor Terminal Voltage Drop

The calculations shown on this sheet were based on data from the system capacity test and rated motor current.

System Representation:



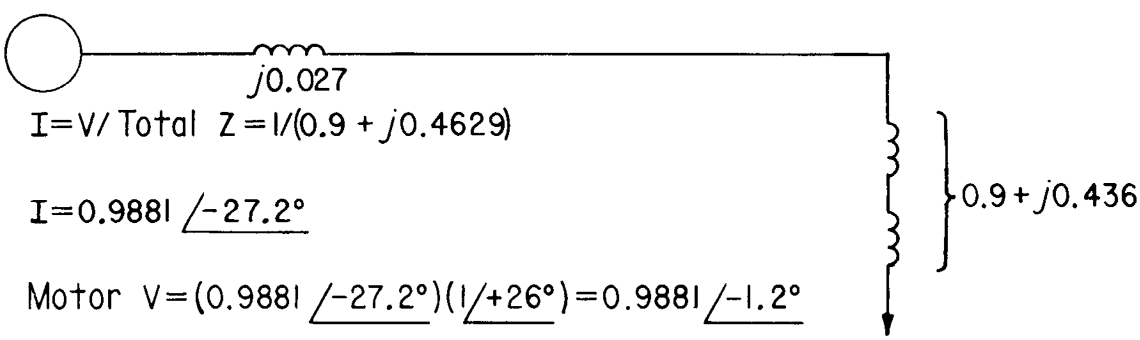
System voltage, $V_S = 1$ pu (per unit)
Starting $I = 960$ A
Rated motor current, $I = 153$ A at 0.9 PF (power factor)
Load per unit, $I = 960/153 = 6.275$ pu
Measured voltage drop on 2400-V bus was 17.0 percent, or 0.17 pu
System impedance, $Z = 0.17/6.275 = 0.027$ ohms per unit

System Voltage Drop At Motor:

For 1 per unit current at rated voltage and motor load,

$$I = 1 \text{ at } 0.9 \text{ PF} \longrightarrow I - 1\angle{-26^\circ}$$

Motor impedance, $Z = 1/1\angle{-26^\circ} = 1\angle{+26^\circ} = 0.900 + j0.436$, where $j = \sqrt{-1}$



$I = V/\text{Total } Z = 1/(0.9 + j0.4629)$

$I = 0.9881\angle{-27.2^\circ}$

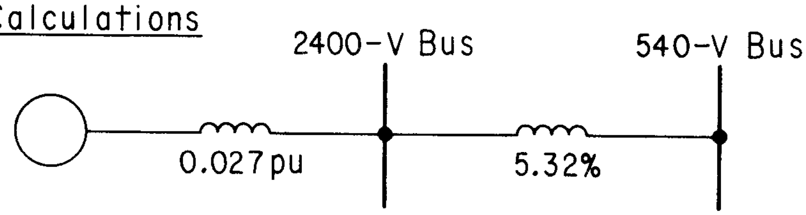
Motor $V = (0.9881\angle{-27.2^\circ})(1\angle{+26^\circ}) = 0.9881\angle{-1.2^\circ}$

System voltage drop $= (j0.027)(0.9881\angle{-27.2^\circ}) \approx 0.027$ pu or 2.7%

Motor voltage drop $= 1 - 0.9881 = 0.119$ pu or 1.2%

CALCULATION SHEET NO. 4

SCR Calculations



System Data
2400 V
153 A
636 MV·A
9.05 ohms

Transformer Data	
2400 V	540 V
48.1 A	214 A
28.8 ohms	0.2 MV·A
0.0532 pu Z	1.46 ohms
	0.0532 pu Z

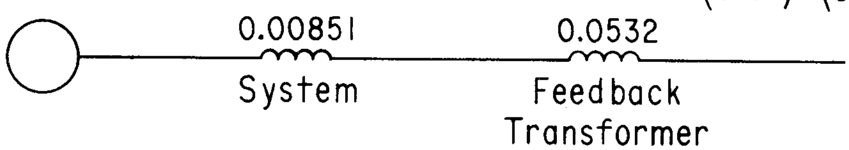
Assume: Converter rating is equal to 0.2 MW, which is the same as the feedback transformer rating.

1. Convert system impedance to feedback transformer base on the low voltage side:

$$\text{Transformer impedance ratio} = (2.4)^2 / (0.54)^2 = 19.75$$

$$\text{Low voltage system impedance} = 0.027 / 19.75 = 0.00137 \text{ pu}$$

$$\text{New impedance per unit} = (0.00137)\left(\frac{2.4}{0.54}\right)^2 \left(\frac{0.2}{0.636}\right) = 0.00851 \text{ pu}$$



Total impedance = 0.0617

2. Calculate SCR:

$$\text{Short circuit } I = 1/0.0617 = 16.2 \text{ pu}$$

$$\text{System capacity} = (16.2)(214 \text{A/pu})(\sqrt{3})(540 \text{V}) = 3.24 \text{ MV·A}$$

$$\text{SCR} = 3.24 \text{ MV·A} / 0.2 \text{ MW} = 16.2 \text{ pu (As is)}$$

CALCULATION SHEET NO. 4 – CONTINUED

3. Calculate SCR for an ideal zero impedance feedback transformer:

   Short circuit $I = 1/0.00851 = 117.5$ pu
   System capacity $= (117.5)(214\ \mathrm{A/pu})(\sqrt{3})(540\mathrm{V}) = 23.5\ \mathrm{MV\cdot A}$
   $\mathrm{SCR} = 23.5\ \mathrm{MV\cdot A} / 0.2\ \mathrm{MW} = 117.5$ pu (System only)

4. Calculate SCR for feedback transformer only, assuming zero system impedance:

   Short circuit $I = 1/0.0532 = 18.8$ pu
   System capacity $= (18.8)(214\ \mathrm{A/pu})(\sqrt{3})(540\ \mathrm{V}) = 3.76\ \mathrm{MV\cdot A}$
   $\mathrm{SCR} = 3.76\ \mathrm{MV\cdot A} / 0.2\ \mathrm{MW} = 18.8$ pu (Transformer only)

5. Calculate SCR for a feedback transformer having one-half the actual impedance and same megavolt-ampere rating. This would be the same as doubling the megavolt-ampere rating and keeping the per unit impedance constant.

   Short circuit $I = 1/(0.00851 + 0.0532/2) = 28.5$ pu
   System capacity $= (28.5)(214\ \mathrm{A/pu})(\sqrt{3})(540\ \mathrm{V}) = 5.7\ \mathrm{MV\cdot A}$
   $\mathrm{SCR} = 5.7\ \mathrm{MV\cdot A} / 0.2\ \mathrm{MW} = 28.5$ pu (Larger transformer)

APPENDIX G

MOTOR STARTUP TESTS

INTRODUCTION

This appendix consists of an analysis and evaluation of the data obtained at Fountain Valley Pumping Plant during the June 15-17, 1983, field test investigation. The purpose of this second investigation was to study in detail the pump motor startup problems and to resolve and/or eliminate the problems which made it impossible to operate more than one unit at a time.

TEST PROGRAM

There were about 14 tests performed involving 5 different plant configurations. The first test series, A, consisted of obtaining benchmark data for the standard plant configuration. These data would be useful as a baseline reference for comparison with data obtained from the other tests. The standard harmonic filter consisted of 160 kΩ and 320 μH.

The second test series, B, involved three tests at a reduced filter capacitance of 100 kΩ per drive. These tests were performed to determine if reducing the filter capacitance would result in improved voltage waveforms during startup.

In test series C, the plant feedback transformers were wired directly to the 2400-V bus. The purpose of this testing was to determine if eliminating the filter and feedback transformer transient current during startup would eliminate the starting problems.

Test series D called for the removal of the filter capacitors. The feedback transformers remained wired directly to the 2400-V bus. These tests would indicate if the capacitors were involved in distorting the 600-V bus voltage waveforms.

The final test series, E, consisted of connecting the filter capacitors at the motor terminals instead of at the drive terminals. The feedback transformers remained connected directly to the 2400-V bus. The purpose of these tests was to determine if relocating the capacitors would improve the steady state and/or transient performance of the system. There was some concern that the location of the capacitors next to the drive was somewhat less than ideal with respect to standard transient suppression, protection, and design practices.

Table G-1 is a listing of the plant configuration for each test series and a description of each test.

INRUSH CURRENT MEASUREMENTS

Table G-2 is a listing of the maximum inrush current measured during each startup attempt. In addition, this table shows the number of units running at the time of the startup attempt and whether or not the attempt was successful. Table G-2 also shows that wiring the feedback transformer directly to the 2400-V bus resulted in lowering the maximum peak inrush current from 1020 to 890 A. The average maximum peak inrush current was reduced from 905 to 735 A. This is equivalent to reducing the peak inrush current by 2.5 per unit on the 2400-V feedback transformer base. Also note that the 905-A peak is 4.2 per unit on the motor base. The 735-A peak is 3.4 per unit on the motor base. The reduction in peak inrush current appears to be related to the success rate of bringing additional units on line. Prior to preenergizing the feedback transformer and filter, 8 of 12 attempts were successful in bringing a second unit on line. After the feedback transformer was wired directly to the 2400-V bus, there were no failures in bringing a second unit on line, and 7 of 10 attempts were successful in bringing a third unit on line. Obviously, reducing the initial current inrush demands improved the pump startup success rate substantially.

OSCILLOGRAMS

One oscillogram from each test series is presented at the back of this appendix. In reviewing these oscillograms, there appears to be, at the time of startup, more distortion in the 600-V bus voltage waveforms when the feedback transformer is connected normally than when it is wired directly to the 2400-V bus. This correlates well with the fact that the system is more stable and the startup success rate higher when the feedback transformer is wired directly to the 2400-V bus. The oscillograph calibrations are presented in table G-3.

VOLTAGE MEASUREMENTS

During run 14, accurate DVM measurements of the 2400-V bus were obtained. These measurements were made to obtain accurate information on the drop in the 2400-V bus voltage corresponding to motor load. The data and calculations are presented in table G-4. The results indicate that the normal unit voltage drop under load is between 1.2 and 1.5 percent. This agrees well with the data and calculations presented in appendix F of this report. The minimal voltage drop data strongly support our contention that the system is of sufficient capacity.

TRANSFORMER DIRECT CURRENT

A special instrumentation device was used to monitor the direct current circulating between the feedback transformer and the solid-state converter. In

Table G-1. – Plant configuration and descriptions of tests run on June 16-17, 1983.

Test series and plant configuration	Run No.	Description of test
A	1A	Starting of unit 4
Filter: 160 kΩ	1B	Unit 4 running when starting unit 3
320 μH	2A	(Same as 1B)
No. 3 drive disabled	2B	Steady-state waveforms of run 2A
	3	Unit 4 running when starting unit 3, at which time unit 4 tripped off-line
	4A	(Same as 1B)
	4B	(Same as 3)
B		
Filter: 100 kΩ	5A	Unit 4 running when starting unit 3
320 μH	5B	(Same as 5A)
No. 3 drive disabled	5C	(Same as 5A)
	5D	(Same as 5A)
	5E	(Same as 5A)
	6	Unit 4 running when starting unit 2, at which time unit 4 tripped off-line
	7A	Unit 4 running when starting unit 2
	7B	Units 2 and 4 running when starting unit 3, at which time both units 2 and 4 tripped off-line
C		
Filter: 100 kΩ	8	Energization of all three feedback transformers
320 μH	9	(Same as 8)
No. 3 drive disabled; all three	10A	Unit 4 running when starting unit 2
feedback transformers hot	10B	Units 2 and 4 running when starting unit 3
wired.	10C	Units 2 and 4 running when starting unit 3, at which time both units 2 and 4 tripped off-line
D		
Removed 100-kΩ capacitors;	11	Energization of all three feedback transformers
all three feedback transformers	12A	Unit 4 running when starting unit 2
hot wired	12B	Units 2 and 4 running when starting unit 3
	12C	(Same as 12B)
	12D	Units 2 and 4 running when starting unit 3, at which time both units 2 and 4 tripped off-line
E	13A	Starting unit 4
Connected 100-kΩ capacitors	13B	Unit 4 running when starting unit 2
at motor terminals	13C	Units 2 and 4 running when starting unit 3
	13D	(Same as 13C)
	13E	(Same as 13C)
	13F	Units 2 and 4 running when starting unit 3, at which time both units 2 and 4 tripped off-line
	14A	Starting unit 4
	14B	Unit 4 running when starting unit 2
	14C	Units 2 and 4 running when starting unit 3
	14D	Units 2, 3, and 4 on-line

Table G-2. – Maximum inrush current measured on each startup attempt during June 16-17, 1983 tests.

Test series and plant configuration	Run No.	Max. inrush current (peak), amperes	No. of units running at time of startup	No. of units tripped off-line
A	1A	990	0	NA
Filter: 160 kΩ	1B	820	1	–
320 μH	2A	920	1	–
	2B	NA	NA	NA
	3	792	1	1
	4A	764	1	–
	4B	905	1	1
B	5A	905	1	–
Filter: 100 kΩ	5B	905	1	–
320 μH	5C	905	1	–
	5D	922	1	–
	5E	1020	1	–
	6	960	1	1
	7A	920	1	–
	7B	920	2	2
C	8	NA	NA	NA
Filter: 100 kΩ	9	NA	NA	NA
320 μH	10A	820	1	–
All three feedback transformers	10B	680	2	–
hot wired	10C	680	2	2
D	11	NA	NA	NA
Removed 100-kΩ capacitors;	12A	890	1	–
all three feedback transformers	12B	750	2	–
hot wired	12C	720	2	–
	12D	707	2	2
E	13A	NA	0	NA
Connected 100-kΩ capacitors	13B	NA	1	–
at motor terminals; all three	13C	820	2	–
feedback transformers hot	13D	707	2	–
wired	13E	537	2	–
	13F	792	2	2
	14A	792	0	NA
	14B	NA	1	–
	14C	665	2	–
	14D	NA	NA	NA

NA indicates "Not Applicable"

view of the high direct current measured, the device was connected to each of the three phases of the transformer to determine if the current into one winding equaled the current out of the other two windings of the wye-connected transformer. The current summation check indicated the instrumentation was indeed working properly and that there was in fact about 10 A of direct current in the 540-V winding of the feedback transformer. This 10-A measurement was for a normal system configuration; the currents were larger for various other system configurations. This direct current was later realized to be due to the unconventional asymmetrical design of the converter. Normally, a converter is designed with symmetry in mind to eliminate circulating direct current and the associated problems.

Table G-3. – Oscillograph calibrations from June 16-17, 1983 tests.

Signal			Alternate Signal			Calibration[1]
Item	Phase	Unit	Item	Phase	Unit	
Current	A	4				339 A = 1 inch p-p[2]
Current	B	4				363 A = 1 inch p-p
Current	C	4				355 A = 1 inch p-p
Voltage	A	2				18 V ≈ 0.4 inch p-p
Voltage	B	2				18 V ≈ 0.4 inch p-p
Voltage	C	2				18 V ≈ 0.4 inch p-p
Current	A	3				355 A = 1 inch p-p
Current	B	3				355 A = 1 inch p-p
						1004 A (peak) = 1 inch (peak)
Current	C	3				363 A = 1 inch p-p
Voltage	A	3	Voltage	A	A	18 V ≈ 0.4 inch p-p
Voltage	B	3				18 V ≈ 0.4 inch p-p
Voltage	C	3				18 V ≈ 0.4 inch p-p
Current	A	2	Current	A	4	363 A = 1 inch p-p
Current	B	2	Current	C	4	339 A = 1 inch p-p
Current	C	2				355 A = 1 inch p-p
Current	delta	4				404 A = 1 inch p-p
Current	rotor	4				726 A = 1 inch p-p
Voltage	A	4				18 V ≈ 0.4 inch p-p
Voltage	B	4				18 V ≈ 0.4 inch p-p
Voltage	C	4				18 V ≈ 0.4 inch p-p
Voltage[3]	AB	Bus				5750 V = 1 inch p-p
Current	ROC[4]	4				391 A = 1 inch p-p

[1] All voltage and ampere calibrations are rms (root mean square), unless noted
[2] p-p = peak-to-peak
[3] line A to line B voltage on 2400-V bus
[4] ROC = rotor overcurrent

The effect of the direct current during switching operations and under inrush conditions has yet to be investigated, but it is presently believed that the direct current can only add to the problem by further increasing the percent distortion factor.

ELIMINATION OF FILTER CAPACITOR

Removing the filter capacitor from the circuit did not improve the startup success rate. This does not mean that the tuned circuit does not contribute to the problem, but does show that the drive system problem is related to current disturbance regardless of whether it is current inrush related and/or filter current oscillation related. This statement is based on the fact that much of the earlier Fountain Valley test data strongly indicated that the current inrush surges initiated filter circuit oscillations that aggravated the problem and contributed substantially to the 540-V bus waveform distortions. In addition, the capacitors were removed only from a starting unit, not the running units. The filters of the running units were still able to oscillate by means of perturbations created from the inrush of a starting unit.

Table G-4. – Digital voltmeter data from run No. 14 during June 16-17, 1983 tests.

Plant condition	DVM Reading on PT of 2400-V bus	Voltage drop
All units off	123.00	
Unit 4 running	121.30	123.0-121.3/123.0 = 1.4%
Units 2 and 4 running	119.38	123.0-119.38/123.0 = 2.9%, or 1.45% per unit
Units 2, 3, and 4 running	118.67	123.0-118.67/123.0 = 3.5%, or 1.2% per unit
Units 2, 3, and 4 running, with unit 2 fully loaded at I = 132A	118.50	123.0-118.5/123.0 = 3.7%, or 1.2% per unit

CONCLUSIONS

The analysis of the June 16-17, 1983, test data indicates that any modification made to reduce the inrush current also reduced the severity of the 600-V bus voltage waveform distortions and thereby improved drive system performance.

It appears that the voltage induced across the feedback transformer is related to the higher frequency components of the inrush current. The harmonic like current in the feedback transformer circuit of a running drive (be it either inrush current related or filter oscillation related) induces a voltage across the transformer and, when superimposed with the 60-Hz system voltage waveform, results in the sum of the two signals appearing on the 540-V inverter side of the transformer. The composite signal is a distorted waveform which produces errors in the gate firing circuits thereby producing phase-to-phase faults in the converter package that trips the equipment off-line. The ability of the drives to ride through this period is dependent on the percent distortion factor of the 540-V bus. Furthermore, the percent distortion factor is a plant design parameter that must be considered in the selection of all associated equipment. To produce a workable system, the equipment must be sized properly to keep the percent distortion factor below the critical limit, above which the equipment will no longer operate properly. In summary, during the initial stages of equipment design, some thought should have been given to equipment integration and the resultant percent distortion factor to ensure proper plant operation. For a detailed analysis of this problem, please refer to appendix F of this report.

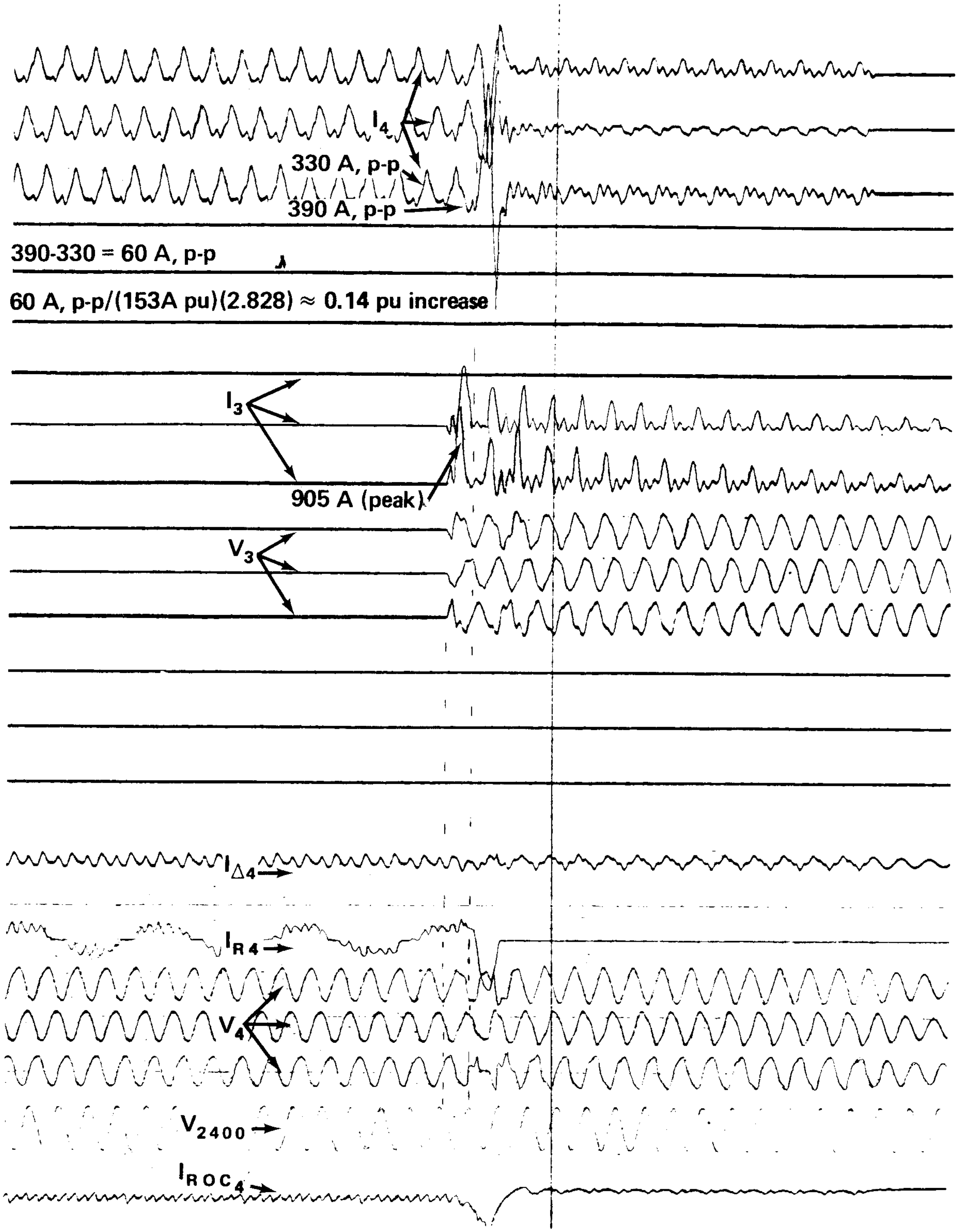


Figure G-1. – Test 4B oscillogram.

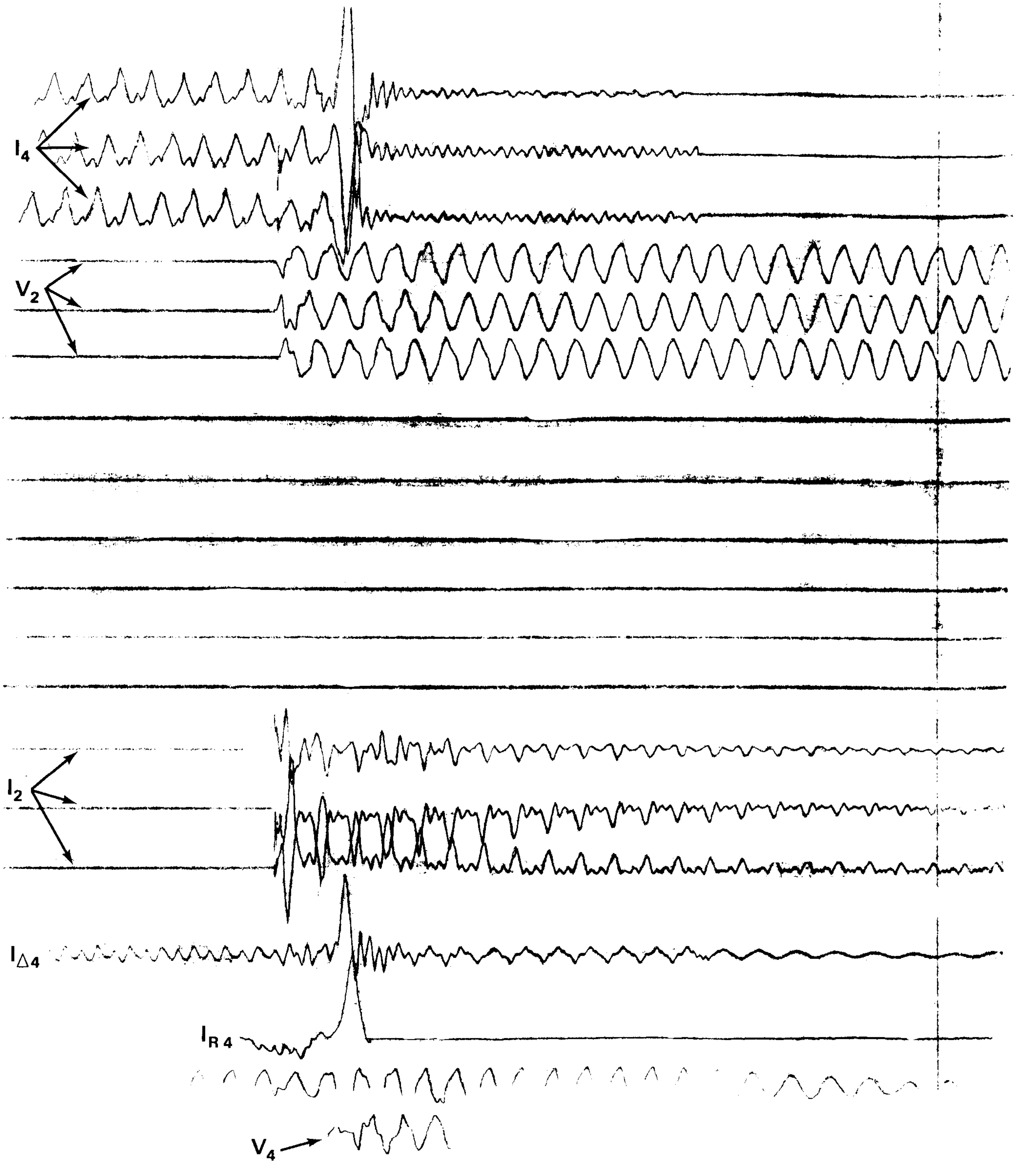


Figure G-2. – Test 6 oscillogram.

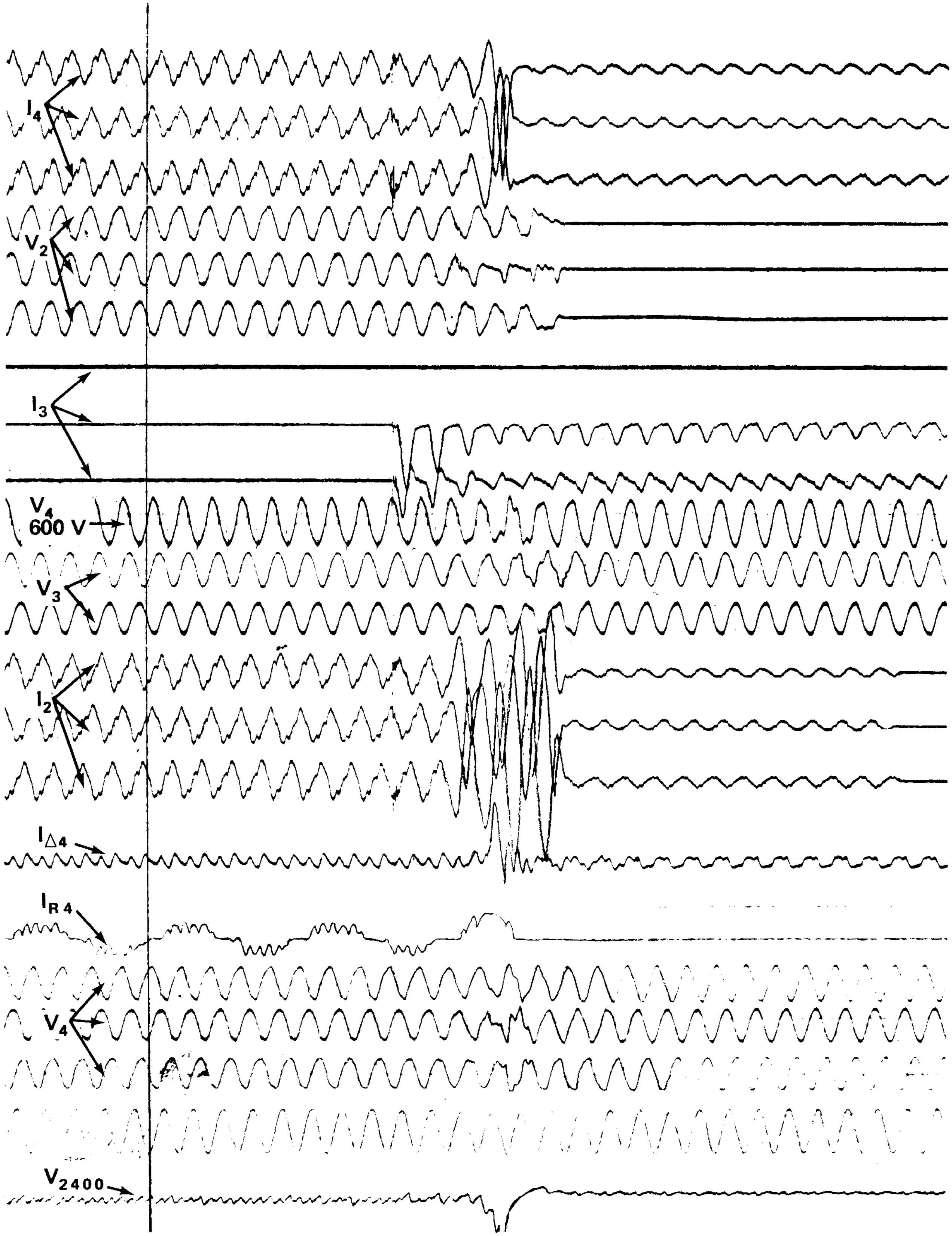


Figure G-3. – Test 10C oscillogram.

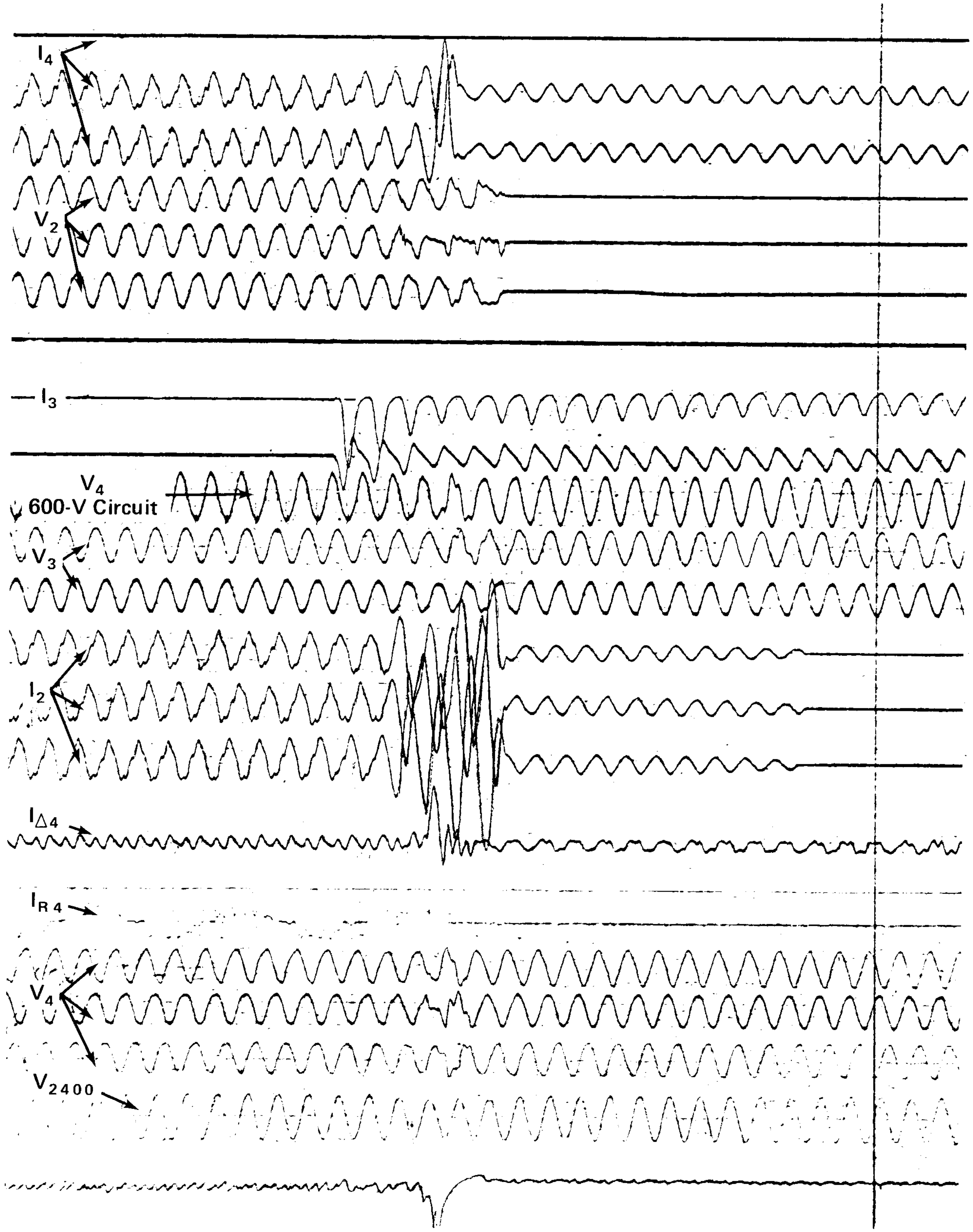


Figure G-4. – Test 12D oscillogram.

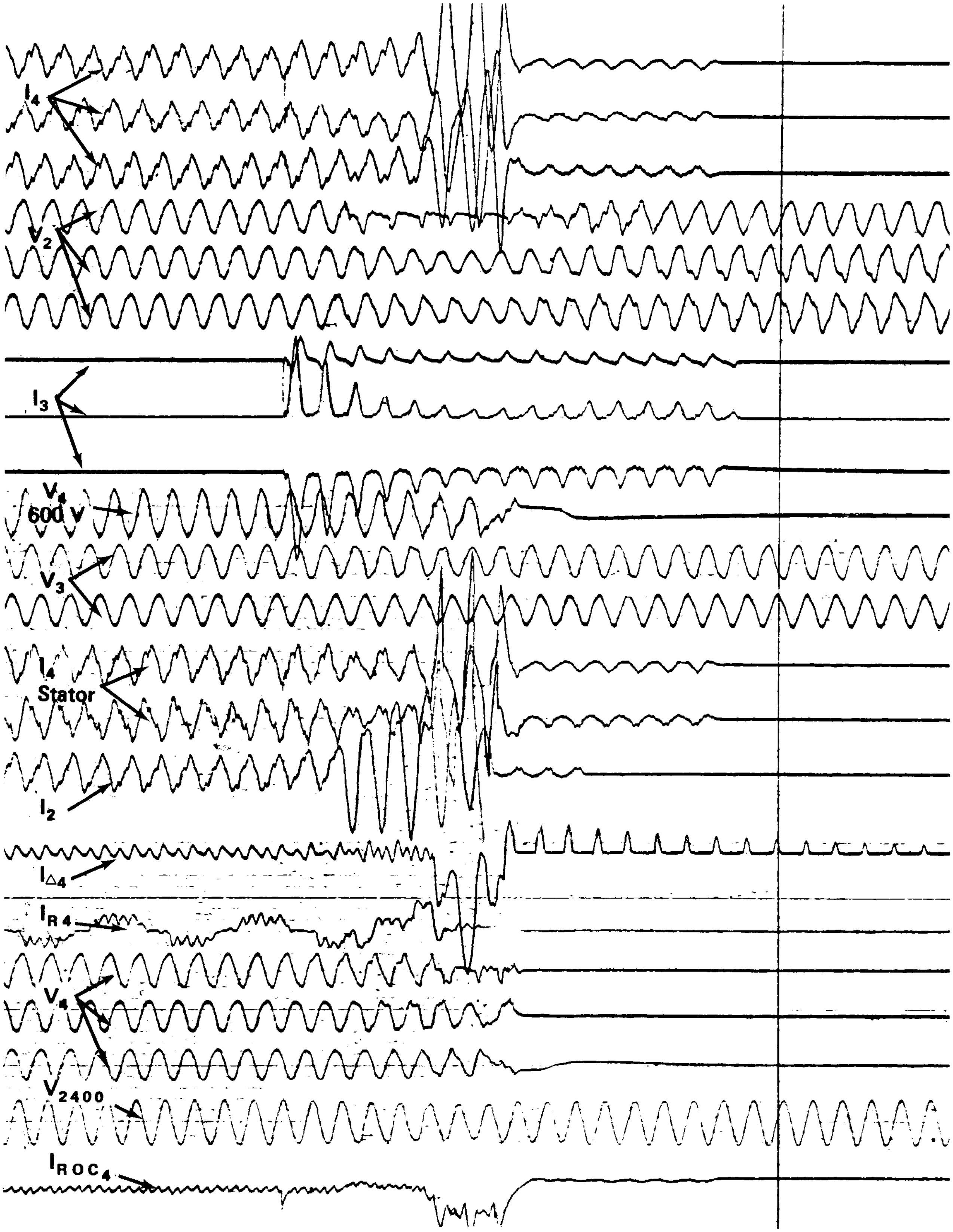


Figure G-5. – Test 13F oscillogram.

APPENDIX H

FIELD EVALUATION OF PROPOSED MODIFICATIONS

INTRODUCTION

This appendix consists of an analysis of the December 6, 1983, Fountain Valley field tests. This particular investigation was undertaken to determine the effectiveness of capacitor modifications to possibly eliminate the startup problems and, if this was not a successful solution, to determine if reconnecting the feedback transformers to the line side of the unit contactors in conjunction with the capacitor modifications would perhaps alleviate the starting problems. The final and most promising alternative option was to investigate insertion resistor starting. This option consisted of energizing the motor stator, feedback transformer, and filter, initially through a current limiting resistor via an auxiliary contactor. Several cycles later, the main contactor would short the auxiliary contactor and resistor. This method of starting should reduce the initial startup current surge and thereby eliminate the related startup problems. This scheme would reduce the 540-V bus SCR requirement and eliminate the startup shock in the harmonic filters of the running units.

TEST SUMMARY

This section is primarily a summary of the December 6, 1983, investigation and consists of the test purpose, results, and a brief conclusion for each test performed. For purposes of quick reference, table H-1 is a condensed version of this test summary. The test numbering designation used in this table refers to the actual oscillogram records. Only the pertinent test oscillograms have been presented on figures H-1 through H-8, and are for reference purposes in reviewing this report. The instrumentation configuration, equipment ratings, circuits, and system configurations are shown on figures H-9 through H-12.

Test No. 1, 9:30 a.m.

System Configuration. – The basic capacitor modifications were made to all three units and consisted of installing 150 kΩ at the motor terminals and 170 kΩ in the filter. The filter inductance was set to 260 μH.

Purpose of Test. – To determine if capacitor modifications alone would be sufficient to eliminate the startup problems.

Test Results. – Unit 2 was on-line and running when unit 3 was started, at which time unit 2 tripped off-line. The unit 2 rotor overcurrent relay operated.

Conclusions. – The capacitor modifications alone were insufficient to eliminate the startup problems. All parties involved agreed to modify the feedback transformer circuits prior to continuing testing.

Tests Nos. 2 and 3, 11:20 a.m. and 11:30 a.m., Respectively

System Configuration. – The capacitor modifications remained the same as in test No. 1. All three unit feedback transformers were connected to the line side of the respective unit contactors.

Purpose of Tests. – To determine if the capacitor modifications in conjunction with the elimination of the feedback transformer inrush would be sufficient to eliminate the startup problems.

Test Results. – Overall, the test results indicated that the capacitor modifications in conjunction with the elimination of the feedback transformer inrush did not eliminate the startup problems.

Conclusions. – Test No. 2 was successful in that all three units were started without any of the running units tripping off-line. However, test No. 3 was unsuccessful in that when the last unit was started, both running units tripped off-line. At this time, all parties involved agreed to proceed with the insertion resistor circuit modifications prior to proceeding with testing.

Test No. 4, 1:55 p.m.

System Configuration. – The capacitor modifications remained the same as in test No. 1, and the feedback transformers of units 2 and 3 remained connected to the line side of their respective unit contactors. The feedback transformer of unit 4 was reconnected to the unit side of the main unit contactor, and the auxiliary contactor and insertion resistor were installed around the unit 4 main contactor.

Purpose of Test. – To determine the correct timing sequence for the auxiliary contactor.

Test Results. – Unit 4 was started twice and the timing sequence adjusted to obtain about four cycles of insertion resistor loading prior to closure of the main contactor.

Conclusions. – Four cycles of insertion resistor time was more than adequate to obtain the desired circuit decoupling on startup.

Test No. 5, 2 p.m.

System Configuration. – Same as test No. 4 except that the drive of unit 4 was intentionally inhibited to decrease testing time and conserve water.

Purpose of Test. – To determine if the insertion resistor scheme would be sufficient to eliminate the startup problems.

Table H-1. – Test descriptions for the December 6, 1983 investigations.

System Configuration	Run No.	Test description
150 kΩ at motor terminals, and 170 kΩ in drive	1A	Starting of unit 2
	1B	Starting of unit 3, at which time unit 2 tripped off-line
Capacitors same as in test 1, and all three feedback transformers wired directly to 2400-V bus	2A	Starting of unit 3
	2B	Starting of unit 2 (no record)
	2C	Starting of unit 4
	3A	Unit 2 running when starting unit 3
	3B	Units 2 and 3 running when starting unit 4, at which time units 2 and 3 tripped off-line
Capacitors same as in test 1. Feedback transformers of units 2 and 3 same as test 2, but unit 4 feedback transformer reconnected as normal except with auxiliary contactor and preinsertion resistor. Drive of unit 4 was disabled.	4A, 4B	Timing tests for auxiliary contactor coordination
	5A	Unit 2 running when starting unit 3, at which time unit 2 tripped off-line
	5B-5G	Units 2 and 3 running when starting unit 4; successful each time
	6A-6E	(Same as 5B-5G)
Drive of unit 4 enabled	7A, 7B	(Same as 5B-5G)
Drive of unit 4 disabled	8A-8J	(Same as 5B-5G)
All three feedback transformers wired directly to 2400-V bus; capacitors same as in test 1.	9A	Initial energization of 2400-V bus
	9B	Steady-state energization of 2400-V bus

Note: Runs 9A and 9B were actually performed prior to test 2

Test Results. – When starting unit 3, running unit 2 tripped off-line. This trip had no impact on the insertion resistor scheme installed on unit 4. The second attempt to start unit 2 resulted in blown startup control fuses. After replacing the fuses, units 2 and 3 were started and brought on-line. Unit 4 was then successfully started and stopped six times, at which point unit 2 tripped off-line due to loss of control power (blown fuses). The blown fuses were traced to a shorted under/over voltage protection relay which was subsequently removed from service.

Conclusions. – The insertion resistor scheme appeared to effectively decouple the starting unit from the running units and seemed to eliminate the startup problems. However, additional testing would be required to statistically ensure that this scheme would work.

Test No. 6, 3:10 p.m.

System Configuration. – Same as test No. 5.

Purpose of Test. – Same as test No. 5.

Test Results. – Units 2 and 3 were on-line and running when unit 4 was successfully started and stopped four times.

Conclusions. – The insertion resistor scheme appeared to be effective in eliminating the startup problems.

Test No. 7, 3:20 p.m.

System Configuration. – Same as test No. 5 except that the drive of unit 4 was not inhibited.

Purpose of Test. – To check the effectiveness of the insertion resistor unit 4 starting scheme through a complete startup sequence.

Test Results. – Units 2 and 3 were on-line and running when unit 4 was successfully started twice and allowed to complete the startup sequence. During tests 5 and 6, the unit 4 start sequence never went beyond the "critical" initial energization stage. This did not affect the validity of the tests because the startup problem occurred only during the initial energization stage of the startup sequence.

Conclusions. – The insertion resistor startup scheme was effective in eliminating the startup problems.

Test No. 8, 3:35 p.m.

System Configuration. – Same as test No. 5.

Purpose of Test. – Same as Test No. 5.

Test Results. – Units 2 and 3 were on-line and running when unit 4 was successfully started and stopped 10 times.

Conclusions. – The insertion resistor scheme was effective, having successfully allowed the starting of a third unit while two other units were on-line and running for all 22 attempts.

Test No. 9, 11:10 a.m.

This test was actually performed prior to test No. 2.

System Configuration. – Same as test No. 2. All three feedback transformers were wired directly to the 2400-V bus.

Purpose of Test. – To observe the system quantities during initial energization of 2400-V bus.

Test Results: Large, rather distorted currents were observed in the lines between the feedback transformer and filter.

Conclusions. – The observed currents were responsible for most of the distortion observed in the 540-V bus voltage waveforms during the initial energization period.

FILTER CIRCUIT REACTION TO STARTUP

During the initial energization period of test 1A, currents of about 1800 A peak were found to exist in the lines between the feedback transformer and harmonic filter. This current was equal to 6 per unit on the feedback transformer 540-V winding current base. As can be seen on the oscillogram (fig. H-1), these currents are highly distorted, large in magnitude, and of relatively high frequency. The period of oscillation is related to the natural frequency of the filter, which is about 200 Hz (3.3 times the fundamental). It can also be seen on figure H-1 that this transient current is responsible for greatly distorting the 540-V bus voltage waveforms. The 2400-V system voltage waveforms, although not clearly reproduced on figure H-1, are only slightly distorted.

From previous tests, it can be assumed that the total 2400-V system maximum current inrush is about 1020 A peak (3.4 per unit on the motor base) and is reduced to about 890 A peak when the feedback transformer is preenergized (refer to appendix G for this data). This indicates there is about 130 A peak inrush to the feedback transformer under normal startup conditions. This is about 2 per unit on the feedback transformer 2400-V winding current base. The motor stator inrush (890 A peak) is about 2.9 per unit on the motor base. The ratio of the measured feedback transformer primary and secondary currents is not equal to the transformer turns ratio because of the nonlinear characteristics that the transformer exhibits during the current inrush period.

The highly distorted high frequency, 6 per unit transformer-filter currents, are the primary cause of the voltage waveform distortions that were observed on the 540-V bus of the unit being started. This 6 per unit high frequency component current will induce a much larger voltage drop across the transformer impedance than the lower frequency (fundamental) current (2 per unit) would in the 2400-V winding. The frequency of this current is important because the transformer impedance increases with an increase in the frequency of the applied current. The objectional drive voltage drop is due to an inadequate SCR at the drive side of the feedback transformer and indicates that the feedback transformer impedance is too high.

In test oscillogram 1B (fig. H-2), unit 2 was on-line and running while unit 3 was being started. The unit 3 inrush current was 1010 A peak. This inrush current not only greatly distorted the starting unit 540-V drive waveforms, but also induced a harmonic like current increase in the filter of running unit 2. During startup, the running unit filter current increased from 330 to 690 A peak (1.1 to 2.3 per unit).

The high frequency components of the current will induce a very large voltage drop (about 10 to 20 percent) on the 540-V bus. This distortion will usually result in silicon-controlled rectifier misfirings and/or commutation failures in the converter. More importantly than the change in the filter current peak amplitude of the running unit is the rather abrupt change that occurs in the actual waveforms. These changes can be seen in oscillograms 1B, 3A, 3B, and 5A, figures H-2, H-3, H-4, and H-5, respectively. Also note that wiring the feedback transformer directly to the 2400-V bus has only slightly reduced the startup induced changes in the transformer-drive circuit current waveforms.

It is also important to note that the current to a running unit, when another unit is started, does not change by more than 0.15 per unit on the motor base (0.45 per unit on the transformer 2400-V winding current base). This current change is a low frequency type change and can be observed on oscillograms 4B and 6 (figs. G-1 and G-2) in appendix G. Since the current is of low frequency and the system and running drive are in steady state, the system voltage drops due to this current change result in a system voltage drop of less than 0.4 percent. Obviously, the distorted drive system voltage is due to the harmonic like current induced reactive voltage drop in the feedback transformer. Again, the low SCR on the drive side of the high impedance feedback transformer appears to be the problem.

INSERTION RESISTOR

The size of the insertion resistor was selected to obtain about 0.5 per unit voltage across the subject resistors during the startup period. The 12.5-ohm resistors selected eliminated the transformer and stator inrush current, and thereby limited the maximum startup current to about 60 A_{rms} (0.4 per unit on the motor base). This current is divided between the motor circuit capacitor and feedback transformer circuit capacitor. The feedback transformer will carry about 30 A of the 60-A total. This is about 0.67 per unit on the feedback transformer base. The calculations involved in selecting the resistor are presented on calculation sheet No. 5 at the back of this appendix.

The actual insertion resistor currents measured during the insertion resistor tests were about 80 A (peak) initially; and 25 A_{rms} steady state. This current is somewhat lower than that calculated and may be due to the heating of the insertion resistors, which were undersized with respect to power rating, and/or the var requirements of the motor stator and filter reactors. However, in the final analysis, the resistor insertion scheme worked well in limiting inrush current and filter current oscillations, as shown on oscillogram 5B (fig. H-6).

DETAILED ANALYSIS ON ENERGIZATION OF 2300-V BUS

This test (test No. 9) was performed after wiring each feedback transformer and filter to the line side of the respective unit contactor. The test consisted of simply monitoring the energization of the 2300-V bus, refer to oscillograms shown on figures H-7 and H-8.

The current in the unit 2, A-phase filter peaked at 790 A (1580 A peak-to-peak). The voltage distortions in all three 600-V circuits being monitored were essentially identical. This implies there was no swapping of vars between units and that the initial current transients in each unit were similar, if not identical, in phase and amplitude. The initial current surges consisted primarily of harmonics, and decayed at a very slow rate (about 1 second).

The third harmonic like distortion on the A-phase, unit 2,600-V bus was greater than 10 percent and eventually dropped to about 2.5 percent. All three unit 600-V bus voltage waveforms were greatly distorted during the initial phase of energization, but improved substantially after the decay of the initial current transient. The voltage waveforms of the 2300-V bus, while slightly distorted during the initial transient period, had considerably lower distortion levels than the voltage waveforms observed on the 600-V bus of each unit.

The unit 2 filter current waveforms improved substantially between the period of the initial surge and steady state. The major change observed in these waveforms was the large decay of the harmonic content. The predominance of what appears to be a third harmonic like component in the initial transient current waveform is to be expected since the filter is tuned to the 3.3 harmonic.

The steady state, unit 2, filter current waveforms were highly distorted and varied from 530 to 620 A peak-to-peak. The calculated line current for the 170-kΩ filter capacitors at 600 V was about 160 A_{rms} (450 A peak-to-peak).

The fact that the filter current waveforms were not similar in shape was rather unusual for a balanced, three-phase, passive power system load. The A-phase current appeared as a sine wave with a slight depression on the leading edge of the positive and negative peaks. The B-phase current trace resembled a triangular waveform, and the C-phase current trace appeared to have been pinched on the trailing side of the positive and negative peaks. We can only conclude that these distortion effects were not due to the third harmonic since a third harmonic component would produce identical effects in each phase and, in addition, the wye-delta transformer to filter connection prevents third harmonic line components and multiples thereof from flowing between the transformer and filter. There are also no even harmonics of any significance in the currents because all three waveforms are symmetrically centered about the horizontal axis. Finally, the fact that the waveforms do not exhibit odd symmetry, $f(-x) = -f(x)$, eliminates the possibility that the 5th, 7th, 11th, etc. harmonics could contribute significantly to the distortions observed.

The B-phase waveform is interesting in that it is practically a triangular shaped waveform and is therefore easy to analyze. Based on a Fourier analysis, the triangular waveform, when broken down into cosine components, has a fundamental component equal to 81 percent of the peak (180 A_{rms}) and 11 percent of the fundamental third harmonic (20 A_{rms}). The 5th and 7th harmonics are 4 and 2 percent of the fundamental, respectively; contributions from higher harmonics can be neglected. Therefore, it appears that the triangular waveform is composed predominantly of 81 percent of the fundamental and 11 percent of the third harmonic. However, since the third harmonic cannot exist and the filter is tuned to 3.3 times the fundamental, we must go with the supposition that the triangular wave must really consist primarily of 81 percent of the fundamental and 11 percent of the higher frequency component (hereafter referred to as the 3.3 component). This is in agreement with the fact that the current waveforms are different from each other in that a third harmonic component would result in identical waveforms, whereas a shift to 3.3 times the fundamental would result in unique and individual waveforms for each phase. The 3.3 component also explains the lack of symmetry observed in each current trace.

The 3.3 component is not an even multiple of the fundamental and would therefore normally result in the current waveforms changing with time as the relative phase between the fundamental and harmonic also changes with time. However, since this does not occur, it must be assumed that the waveforms are synchronized to the fundamental each half cycle. In exploring this assumption, it must be realized that the system is in equilibrium; i.e., the system is in steady state. This implies that the tuned circuit, to perpetually produce the 3.3 component, must be continuously receiving power from the power system to account for the inherent circuit losses. Therefore, it follows that the fundamental frequency is the synchronous driving function that synchronizes the 3.3 component each half cycle.

In support of the previous analysis, the filter current waveforms were duplicated on paper from only the fundamental and 3.3 component. In the graphical construction, the 3.3 cosine component was assumed to be synchronized to the B-phase cosine fundamental. This assumption was necessary to obtain a B-phase triangular waveform. The three composite waveforms were generated by adding the fundamental to 11 percent of the 3.3 frequency component. This produced fairly accurate reproductions of the B- and C-phase waveforms but failed to produce the correct A-phase waveform. However, a nodal analysis of the circuit indicated that to preserve current balance, the A-phase waveform should be composed of the fundamental minus two times 11 percent of the 3.3 component. This resulted in a very good reproduction of the A-phase current waveform. The reproductions are shown at the top of figure H-13. The actual oscillograph current trace waveforms obtained during test 9B (fig. H-8) are shown at the bottom of figure H-13.

Referring to the circuit shown on figure H-14, it can be seen that the waveforms previously constructed can be obtained by simply adding a current source in phase C. The fact that this effect can be produced with only a single phase supply suggests that the capacitor filter is oscillating in a single phase mode about the current loop shown on this figure.

The connection of the feedback transformers in a delta-wye configuration prevents third harmonic line currents and voltages from being exchanged from unit to unit or from the system to individual units. In addition, the feedback transformer is of the core design and therefore has a very high reluctance to third harmonic flux, which will greatly assist in preventing third harmonic voltage distortions. The required third harmonic transformer exciting current can circulate in the delta of the transformer to ensure a pure flux wave and thereby prevent third harmonic voltage distortion in the transformer voltage waveforms. All of this analysis supports the contention that the 3.3 component currents are the cause of the initial and prolonged distortions observed in the 600-V system waveforms.

The 3.3 component currents will induce 3.3 component voltages in both the primary and secondary of the feedback transformer. Obviously, because of the turns ratio and other factors, the influence in the higher voltage primary will be reduced considerably with respect to the secondary. However, the transformer delta-wye configuration and core type construction would not prevent the 3.3 component voltages induced in the primary of the feedback transformers from interacting with the system or other units. In fact, unit interaction could be quite severe and possibly cause a swapping effect or even perpetuate the initial transient period. To more fully explain this analysis, consider the case when all three transformers are energized simultaneously from one source. The initial inrush and phase sequence in each filter unit will be identical and the 3.3 component current of each unit will act in harmony to induce a large 3.3 component voltage on the 600-V system. This is exactly what happened in the subject oscillograph records (figs. H-7 and H-8). Now consider what happens when each feedback transformer is individually energized. The phasing of the initial inrush and phasing of the 3.3 component will vary at random (dependent on the random switching angle). The possibility will exist that two separate units with out-of-phase 3.3 component oscillations could interact to create a swapping effect and/or to produce sustained large amplitude 3.3 component currents. This may have been what occurred during one of the first Fountain Valley investigations, performed by the manufacturer, when very large sustained voltage and/or current distortions occurred. The manufacturer's oscillograms documenting the interactions are shown in appendix C. The 3.3 component currents, if large enough, may also be capable of keeping the feedback transformer in a prolonged nonlinear state of operation as evidenced in oscillogram 9A, figure H-7. For additional comments on this, refer to the following section on harmonic measurements.

HARMONIC MEASUREMENTS

A third harmonic percent measuring device was connected directly to the 540-V bus, line to neutral, to monitor the percent of third harmonic on the drive bus. The device saturates at readings greater than 10 percent distortion and has a response time of 5 to 10 cycles. The output is a 180-Hz, a-c signal that is a percentage of the third harmonic with respect to the fundamental. The signal can be observed on the oscillograph records.

The rather high percentage of third harmonic recorded on oscillogram 1A (fig. H-1) was, at first, somewhat difficult to understand. However, when considering the circuit connections, it becomes obvious that the third harmonic component of inrush current that circulates in the delta-connected winding of the feedback transformer has induced a third harmonic line-to-ground voltage in the 540-V, wye-connected windings. Since the third harmonic line-to-ground voltages were in phase, there would be no third harmonic line-to-line voltages appearing across the delta connected drive potential transformers. This is, of course, assuming there is no interaction from the filter circuit.

Since the feedback transformer 2400-V winding is connected in delta, it is obvious that third harmonic voltages from the system will not be coupled into the drive circuit. In addition, third harmonic line currents cannot flow from the system to the feedback transformer. As a result, the harmonic distortion meter will only respond to third harmonic circulating currents in the delta. That is, the meter, as connected, would respond only to feedback transformer nonlinear conditions such as inrush, saturation, or ferroresonance.

On oscillogram 9A (fig. H-7), there appears to be a prolonged period of nonlinear operation of the feedback transformer, and this period is too long for simple transformer inrush. Oscillogram 1A (fig. H-1) is typical of a normal transformer inrush period. Therefore, it can only be concluded that the system is in resonance. Since this mode of operation at Fountain Valley is neither being used nor being proposed, this problem is of minor concern at this time.

SUMMARY OF RESULTS

Test 1, designed to investigate the effectiveness of the capacitor modifications, was unsuccessful because the running unit tripped off-line when another unit was started. After this test, it was decided to proceed with the second test option, which consisted of eliminating transformer inrush current by rewiring the feedback transformers to the line side of the unit contactors. It was believed that this circuit change, in conjunction with the capacitor modifications, would possibly eliminate the startup problems. With the plant so configured, the first attempt (test 2) was successful in that all three units were successfully started. However, during the second attempt to bring the third unit on-line (test 3), the other two running units tripped off-line. It was then decided to proceed to the next test option of installing the insertion resistor starting scheme on the last unit to be started, unit 4. In this configuration, the feedback transformer of unit 4 was returned to normal so that the motor stator, filter, and feedback transformer would be energized together, first through the auxiliary insertion resistor contactor and then through the main unit contactor.

While waiting for the modifications to be made, the drive manufacturer representative informed the Bureau test crew that the drive manufacturer did not believe that this or any other type of modification would enable the Bureau to reliably start all three units. The manufacturer believed the only solution to the problem was to increase the system capacity and stated this in a letter to the Bureau.

After the required modifications were made, test 4 was performed to obtain the proper timing information for the auxiliary insertion resistor contactor. The rotor inverter circuit of unit 4 was then inhibited to shorten testing time and to save water. This did not interfere with the validity of the testing in that the startup problem occurred during the initial energization of the stator, filter, and feedback transformer and not during the rotor energization period, which occurs about 20 seconds later in the startup cycle.

Tests 5, 6, and 8 consisted of energizing and deenergizing the unit with the special insertion resistor and contactor while the other two units were on-line. In all, 20 successful attempts were obtained without a single failure. There was a problem with control circuit fuses blowing, but this was traced to the unrelated failure of an under/over voltage protection relay, which was removed from the circuit and testing resumed. Test 7 was similar to tests 5, 6, and 8 except that the rotor circuit on the unit with the insertion resistor (unit 4) was now enabled, thereby allowing a complete start sequence while the other two units were running.

CONCLUSIONS

The capacitor modifications with or without rewiring the feedback transformer to the line side of the unit contactor were not sufficient to eliminate the motor startup problems.

The insertion resistor startup technique was very successful in eliminating the motor starting problems. There were 22 successful starts out of 22 attempts. Based on the success of these tests, it is strongly suggested that all of the units at Fountain Valley be modified for insertion resistor starting to eliminate startup related problems.

The test investigation again indicates that the system capacity is indeed sufficient. The startup problems revolved around the high feedback transformer impedance, which severely limits the drive SCR to the point that the drives will not function properly. The insertion resistor scheme eliminates the startup problem by reducing the SCR requirements of the drive.

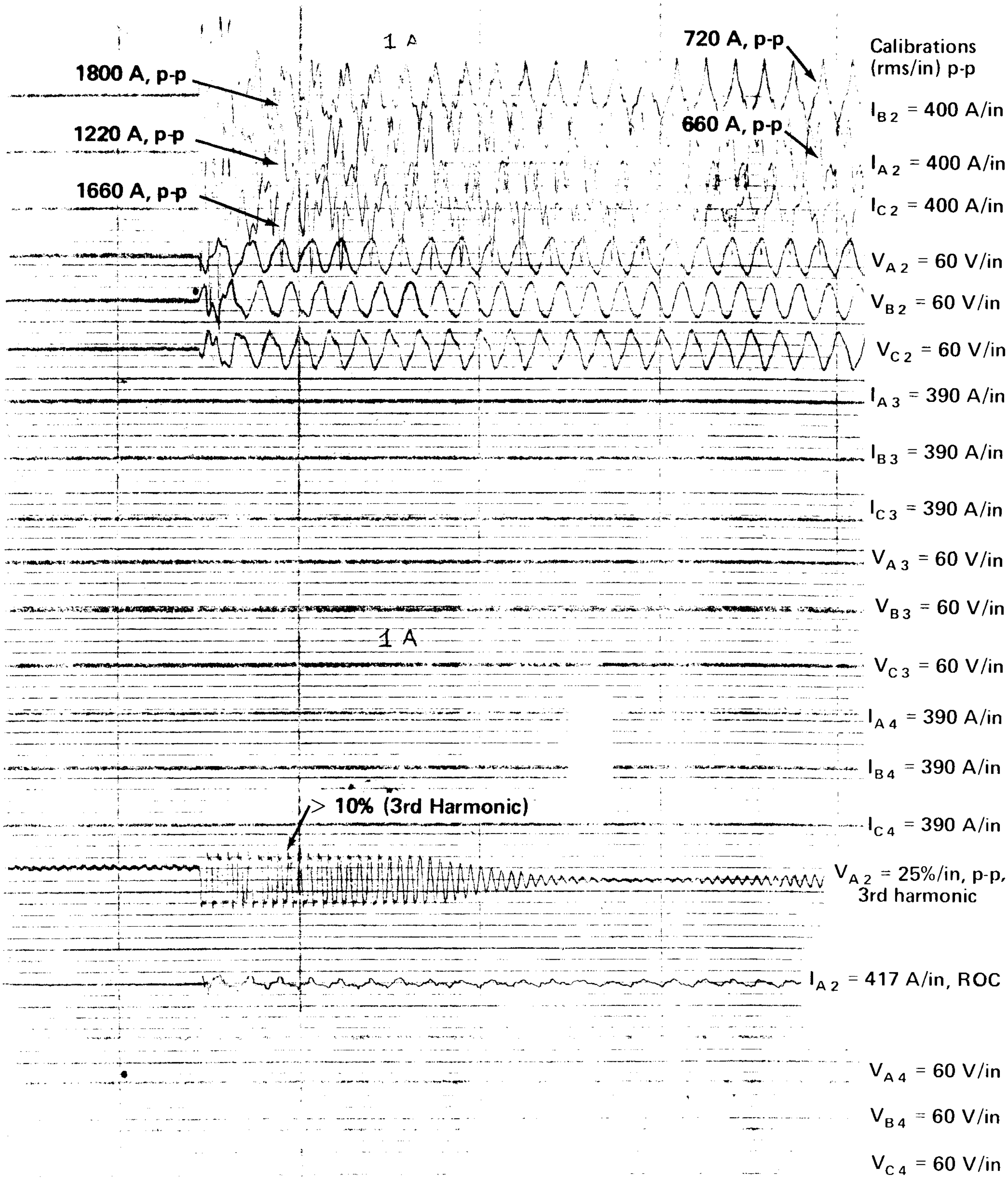


Figure H-1. – Test 1A oscillogram.

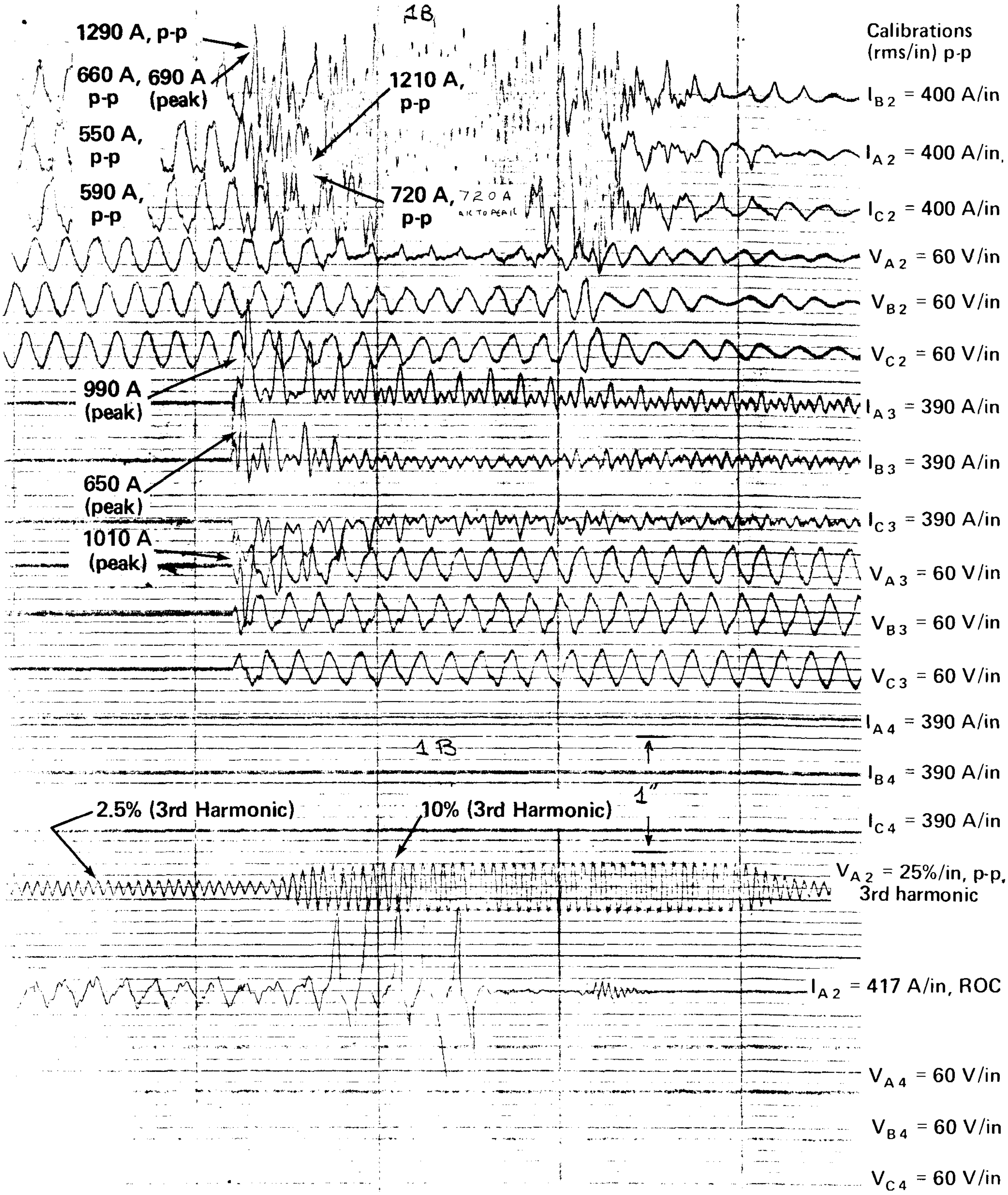


Figure H-2. – Test 1B oscillogram.

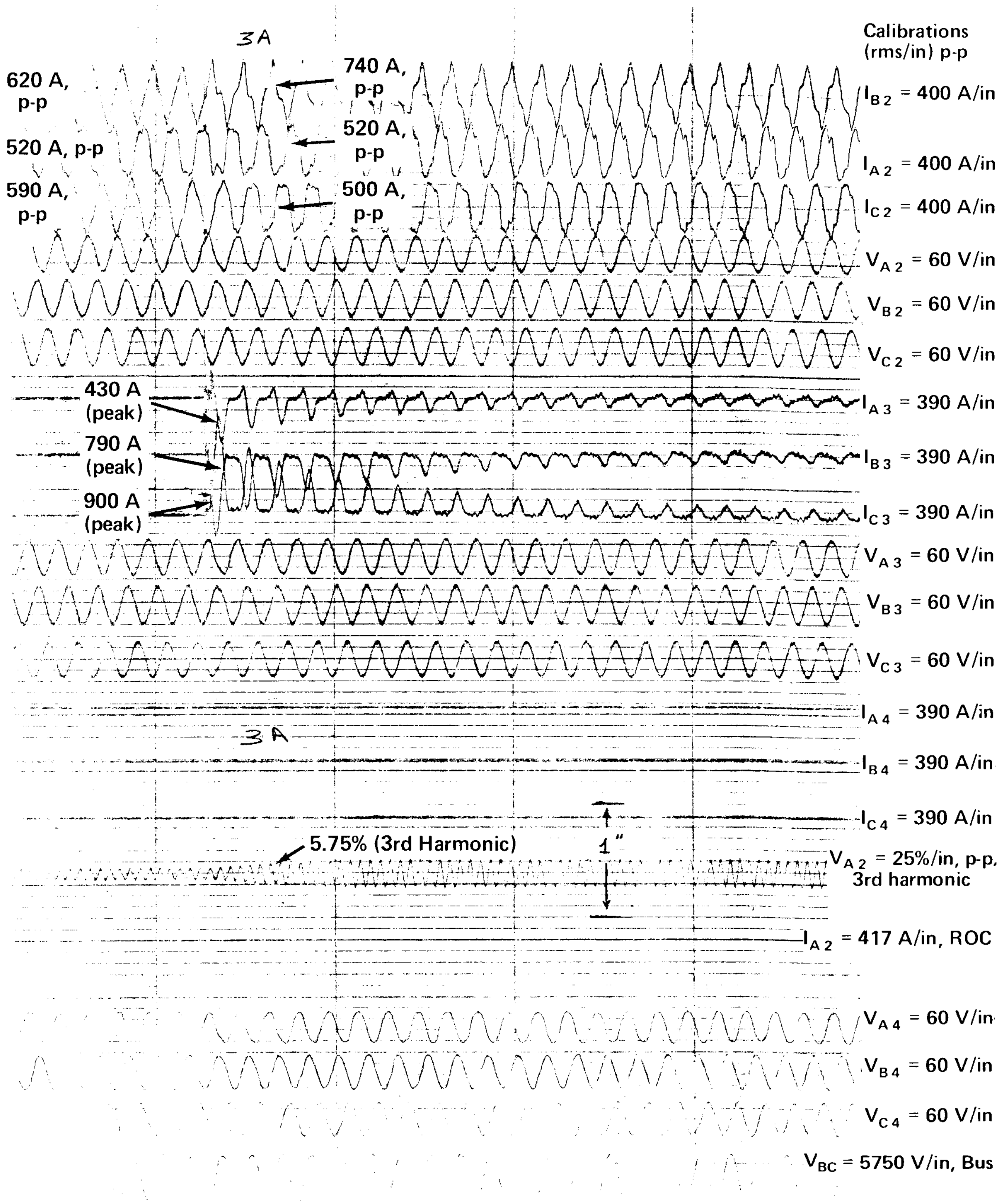


Figure H-3. – Test 3A oscillogram.

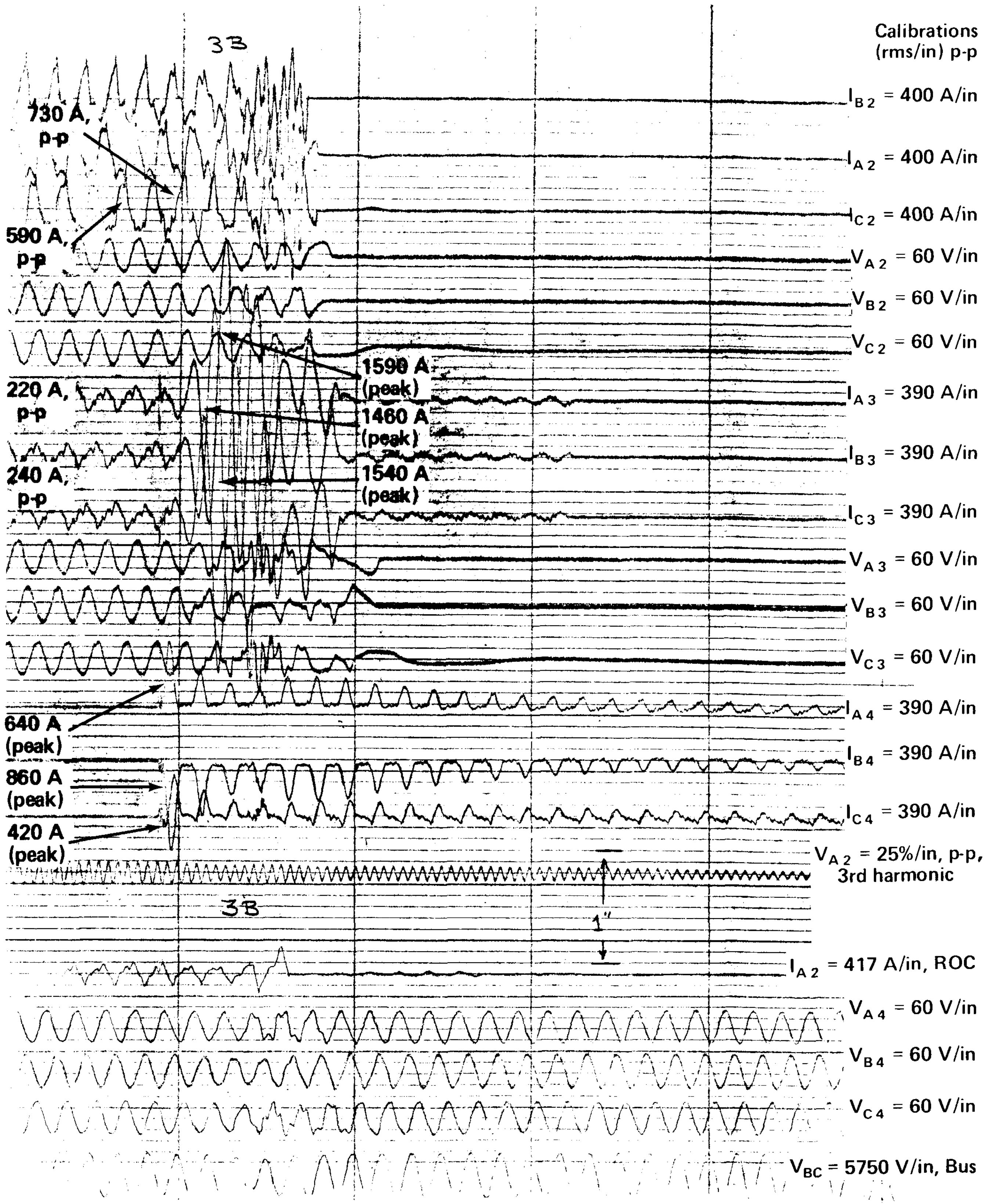


Figure H-4. – Test 3B oscillogram.

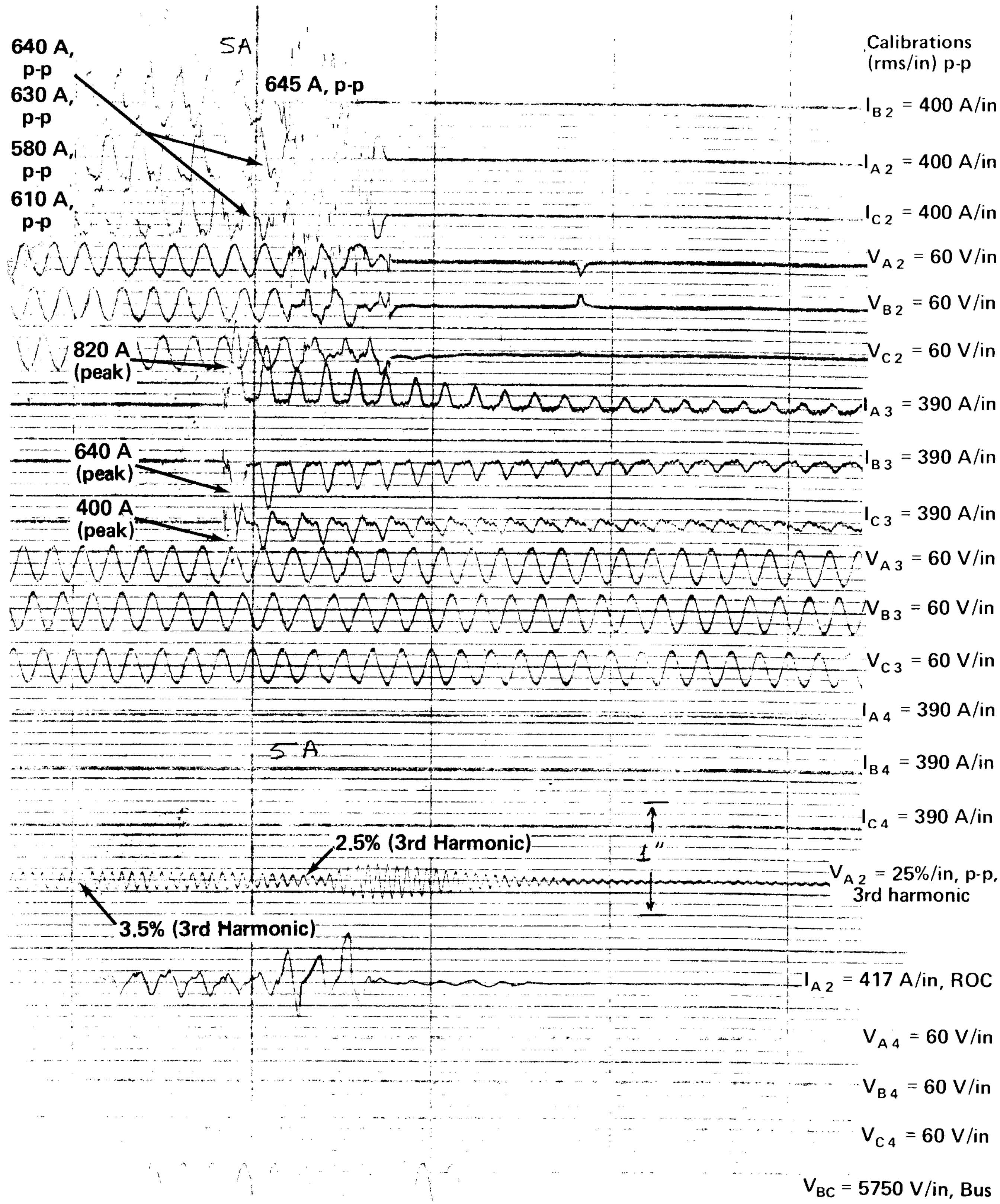


Figure H-5. – Test 5A oscillogram.

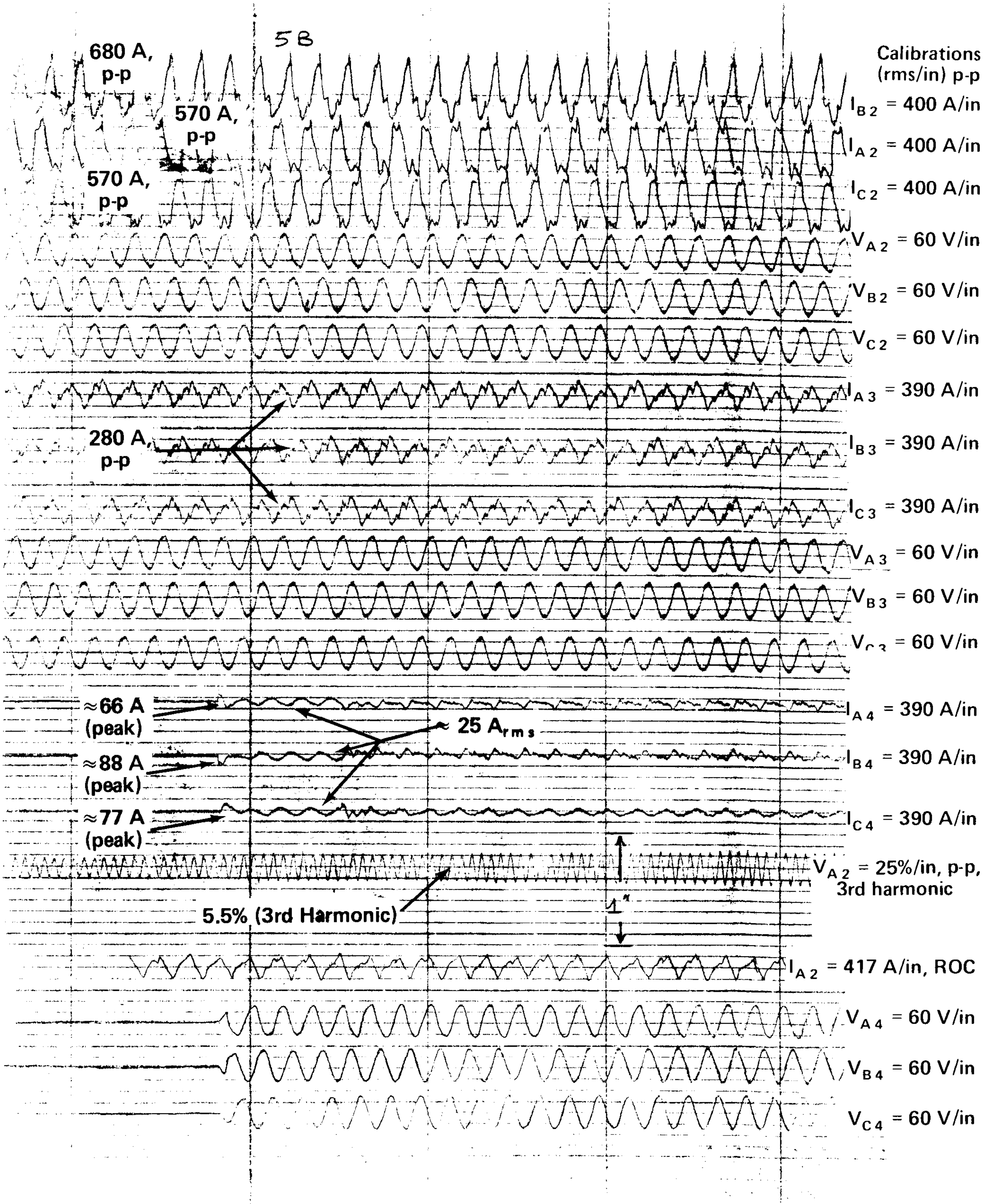


Figure H-6. – Test 5B oscillogram.

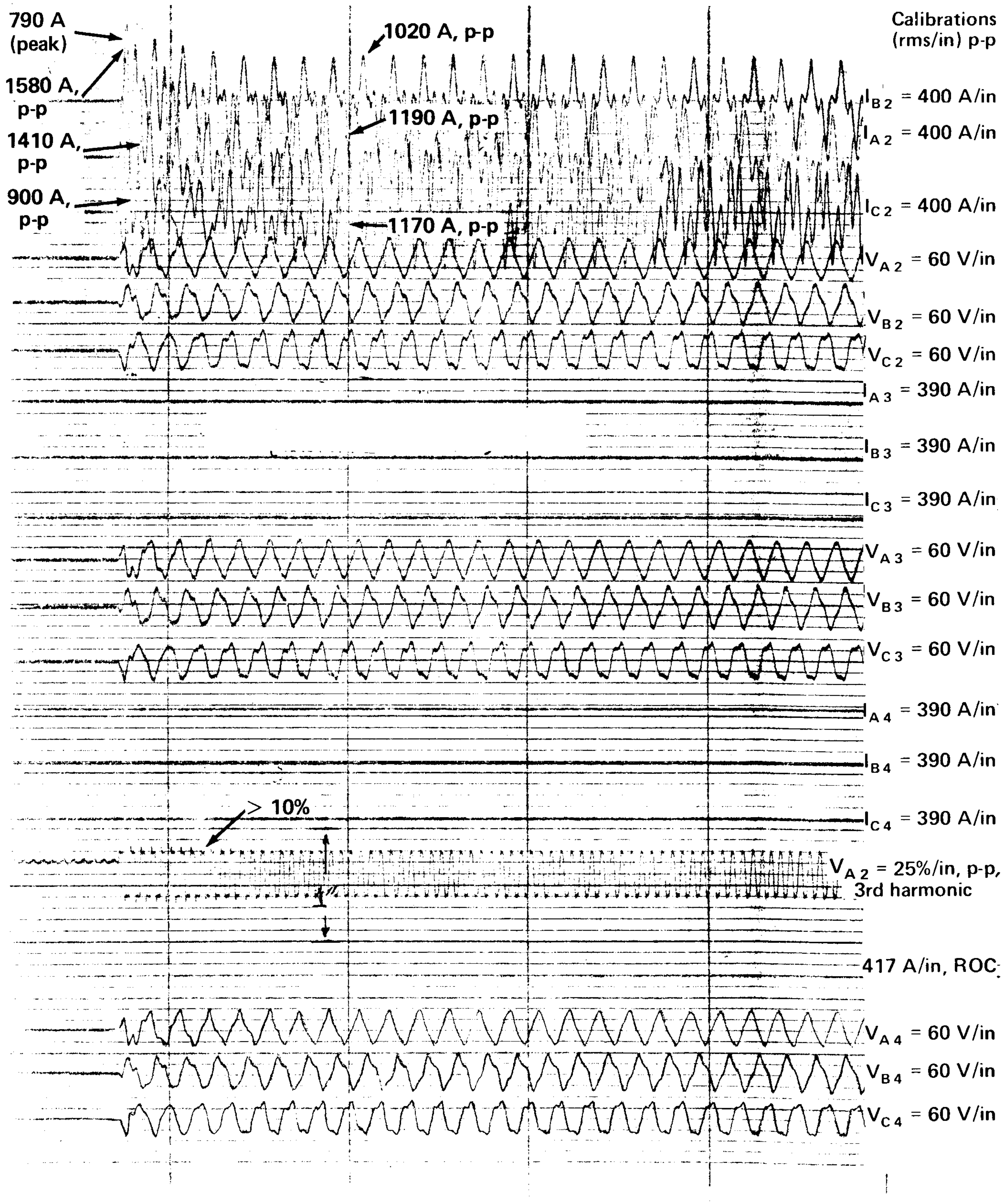


Figure H-7. – Test 9A oscillogram.

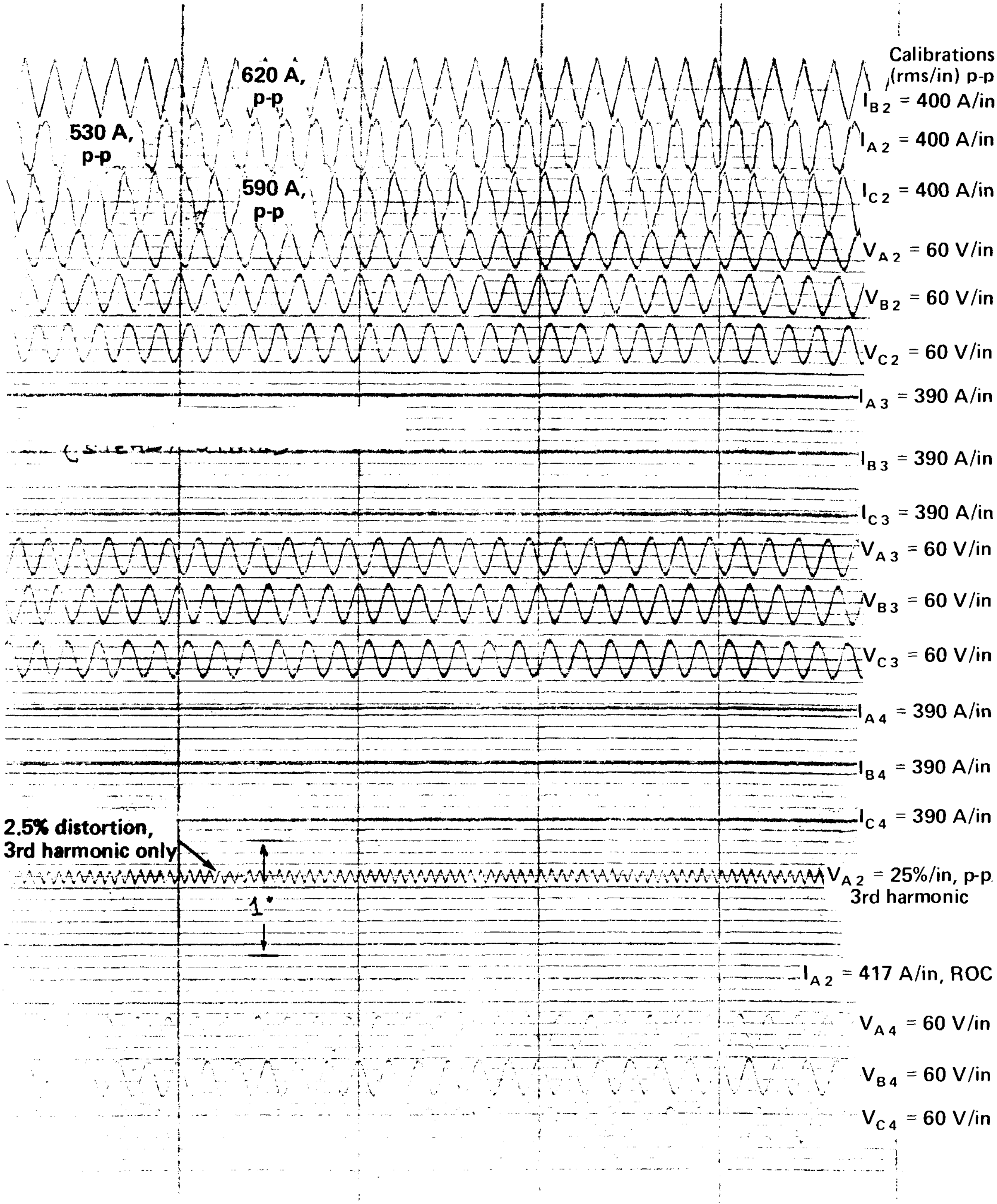


Figure H-8. – Test 9B oscillogram.

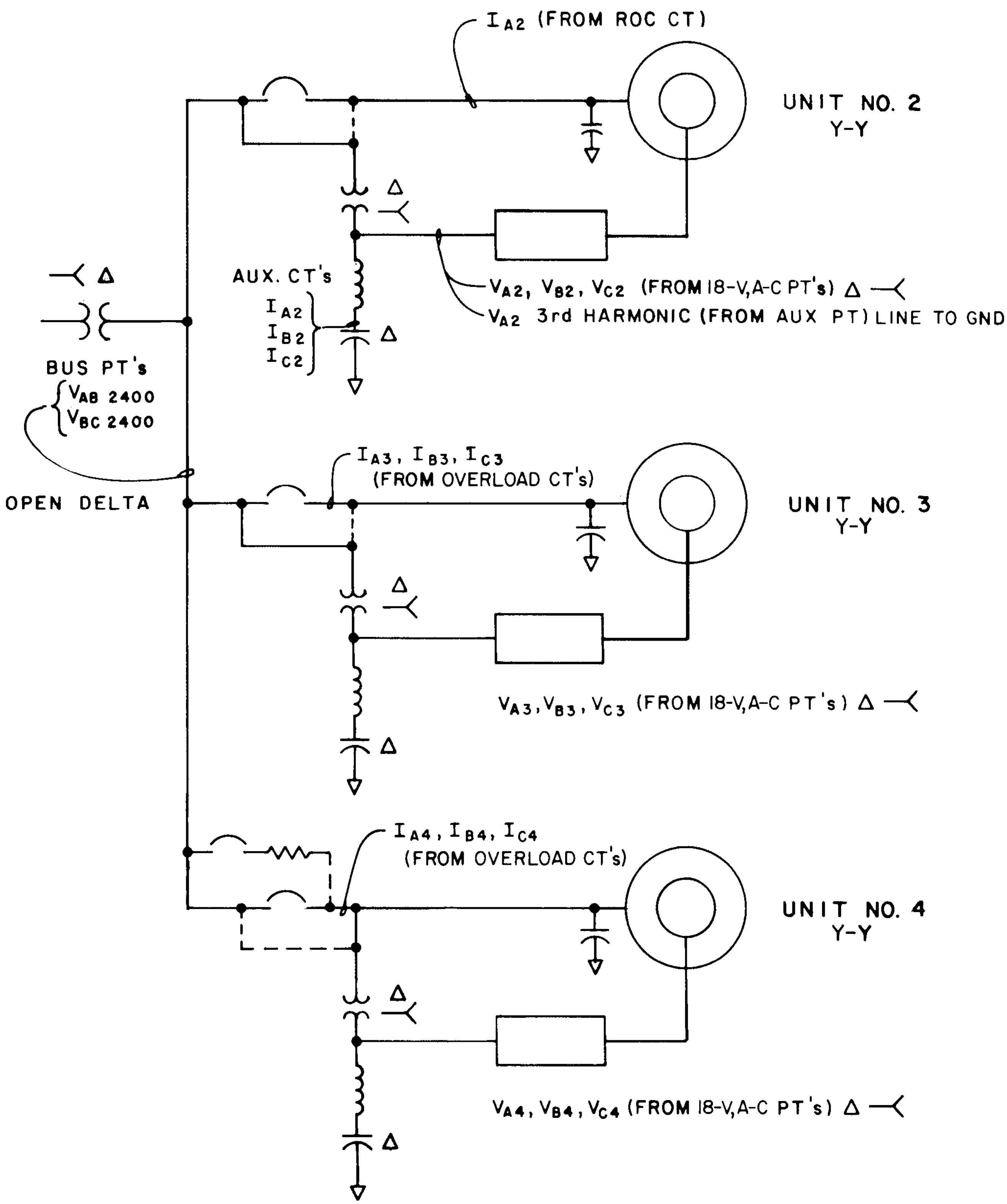


Figure H-9. – Instrumentation.

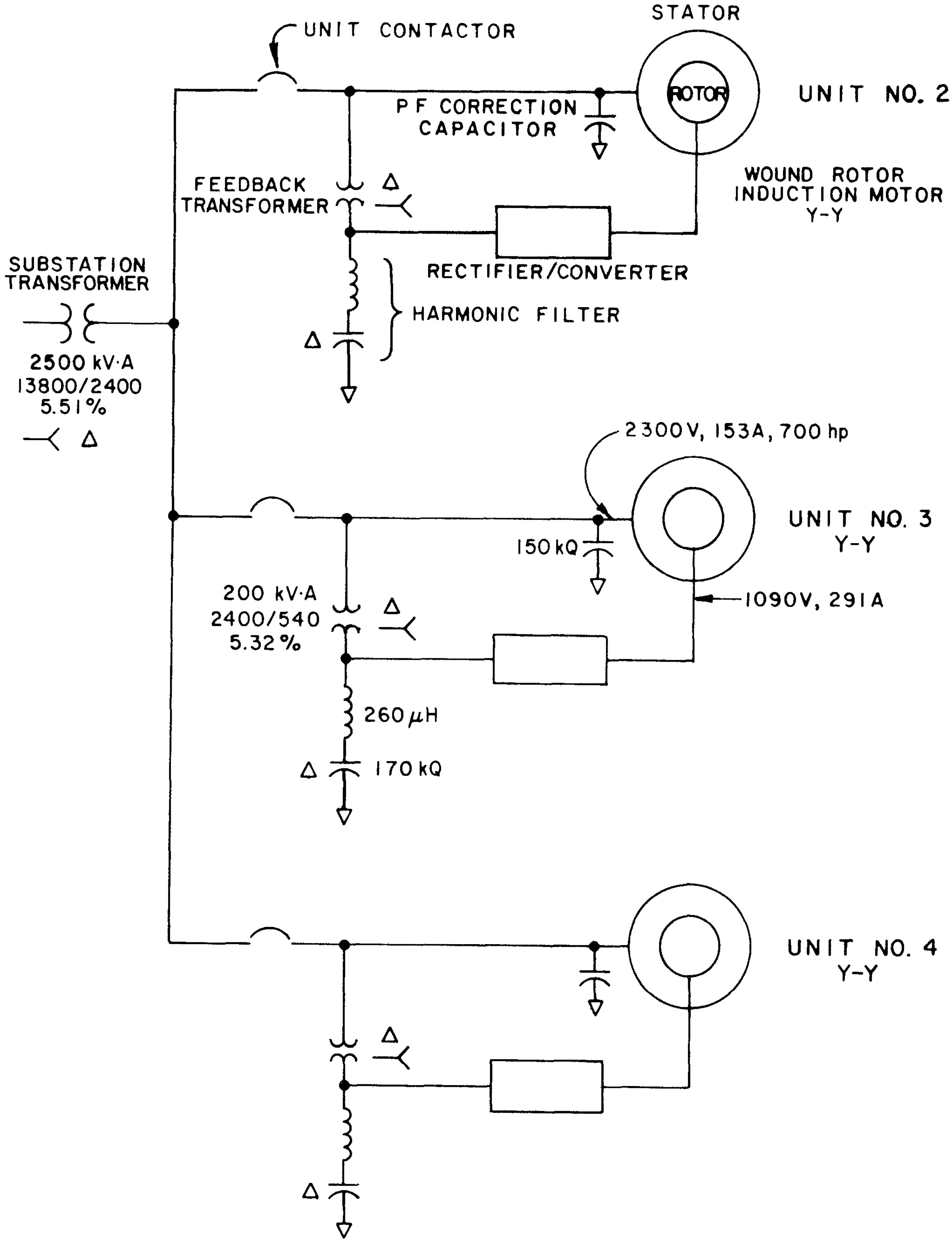


Figure H-10. – Normal system configuration.

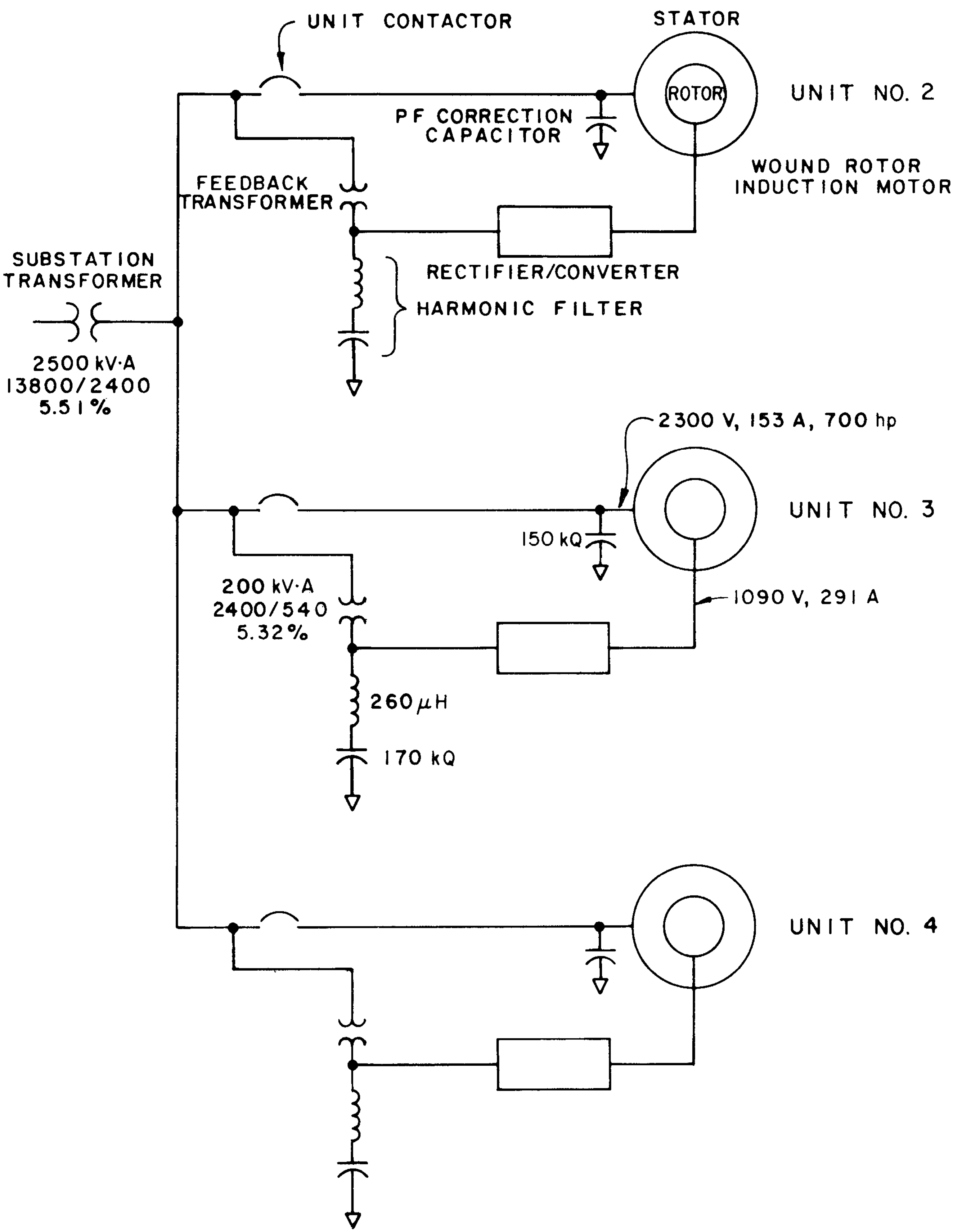


Figure H-11. – Alternate feedback transformer configuration.

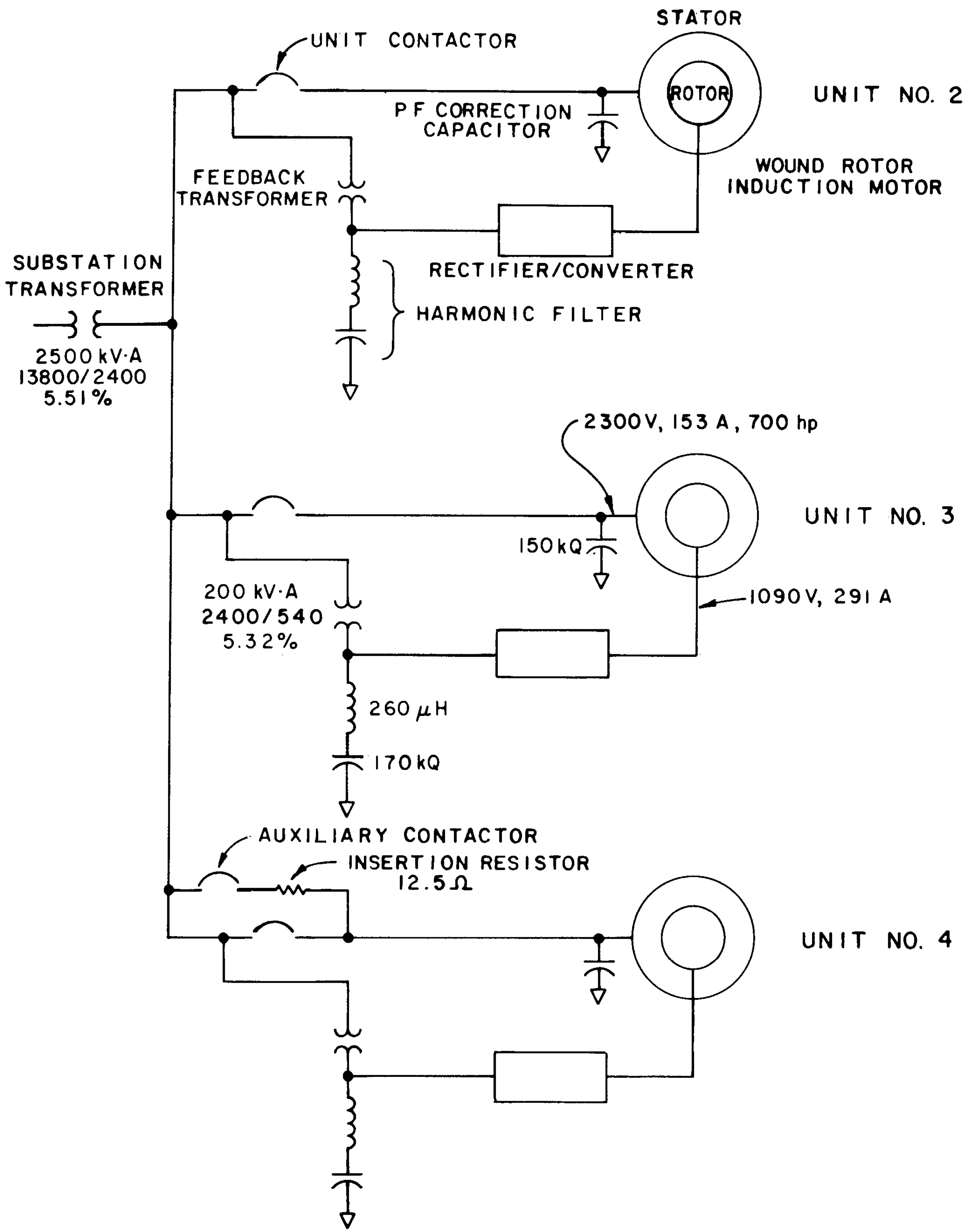


Figure H-12. – Insertion resistor configuration.

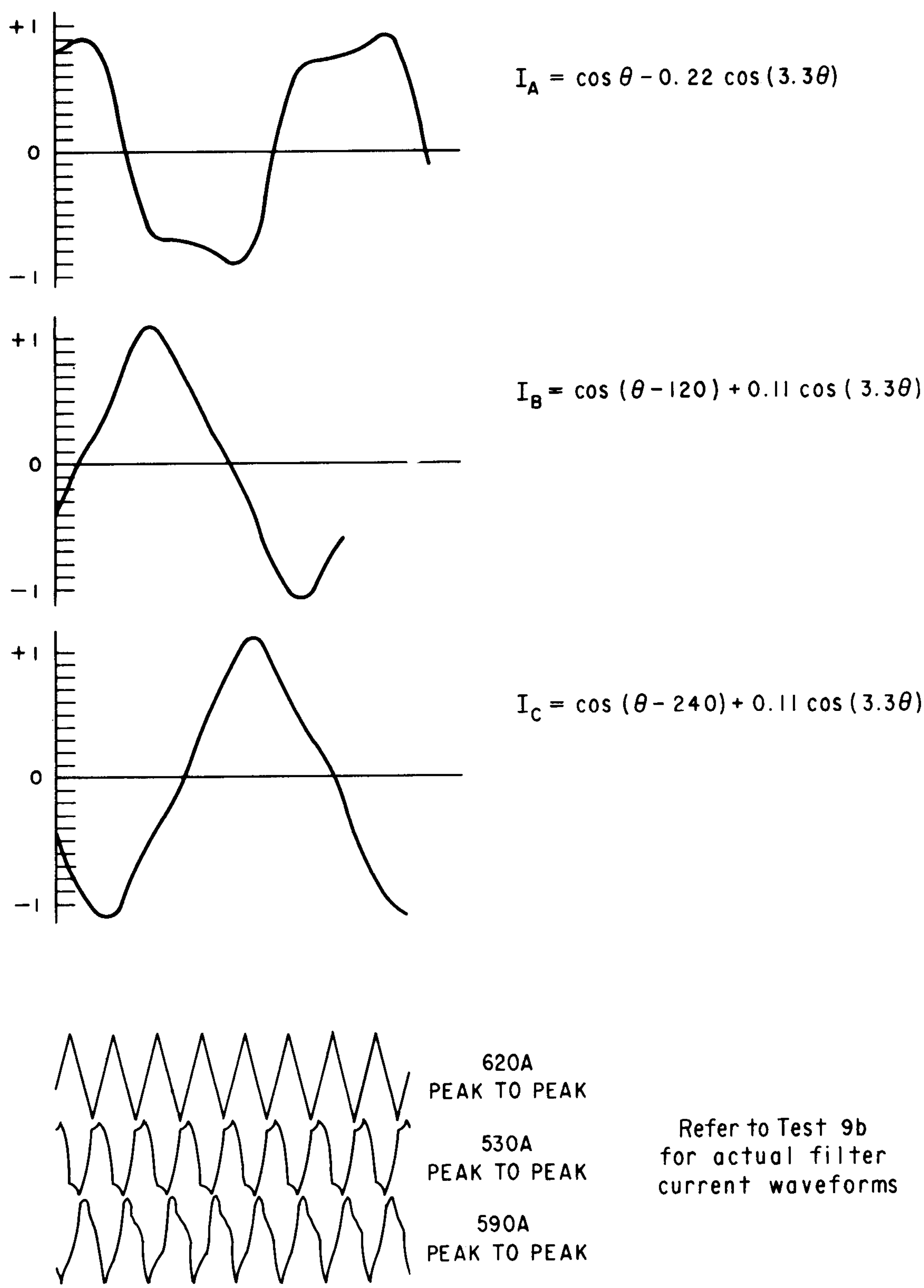
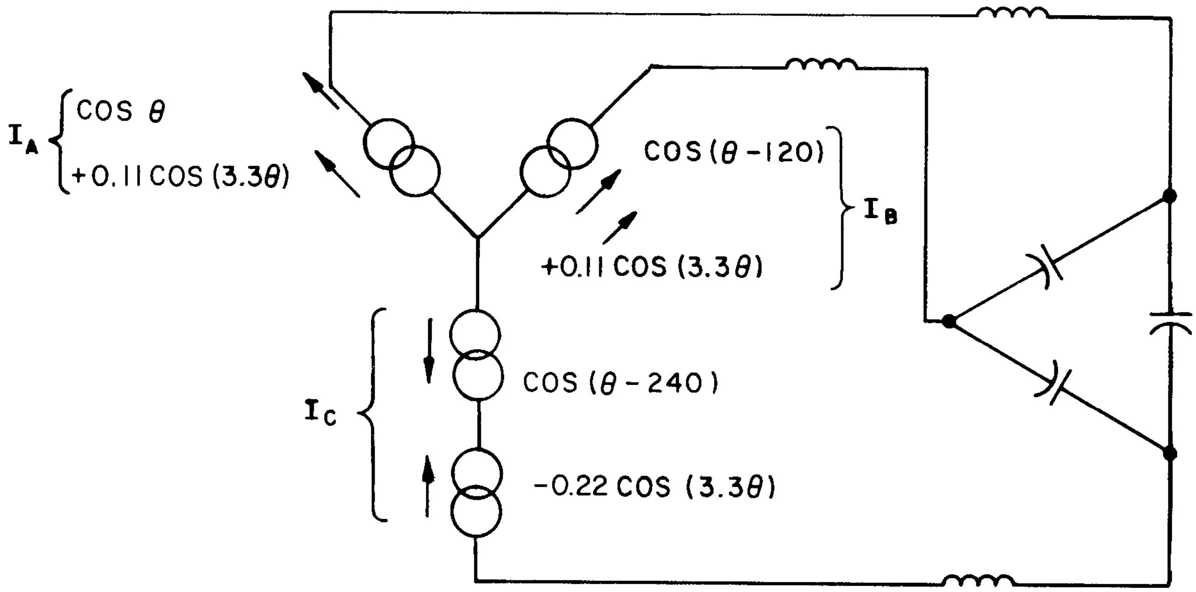


Figure H-13. – Worksheet for harmonic analysis.



I_A = COS θ +0.11 COS (3.3θ)

I_B = COS (θ −120 θ)+0.11 COS (3.3θ)

I_C = COS (θ −240 θ)-0.22 COS (3.3θ)

In all instances, the COS (3.3 θ) term is resynchronized to the I_A COS θ waveform each half cycle at the time of peak amplitude.

Figure H-14. – System model developed from harmonic analysis.

CALCULATION SHEET NO. 5

Insertion Resistor Selection

Line-to-Line voltage = 2400V $\longrightarrow$ Line-to-Ground voltage $\approx$ 1400V

Since the magnetic circuit inrush current will be eliminated by the insertion resistor, only the unit charging current needs to be considered:

$$
\begin{aligned}
Q(\text{Unit}) &= 170\ \text{kQ (drive)} + 150\ \text{kQ (motor)}\\
&= 320\ \text{kQ (3-phase total)}\\
&= 107\ \text{kQ per phase}
\end{aligned}
$$

$$Q = \text{VAR(volt-amperes reactive)}$$

Charging current at startup:

$$I \approx 107 / 1400 \approx 75\text{A}$$

Charging impedance at startup:

$$Z \approx 1400 / 75 \approx 20 \text{ ohms}$$

Selecting 12.5 ohms for the insertion resistor,

$$Z \approx 12.5 - j20 \approx 23 \angle{-58^\circ} \text{ , where } j = \sqrt{-1} \text{, and}$$

the current at startup would be

$$1400 / 23 \approx 60\text{A.}$$

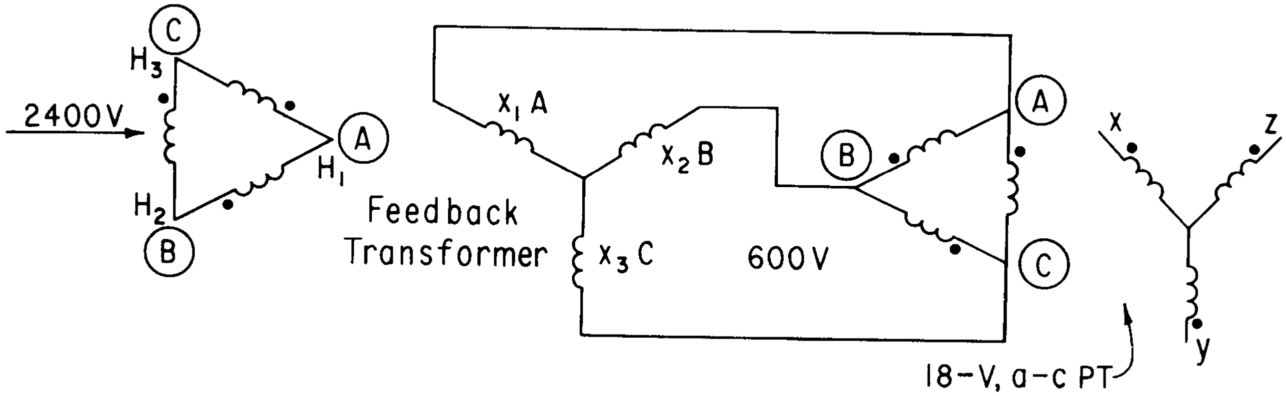
Resistor voltage at startup:

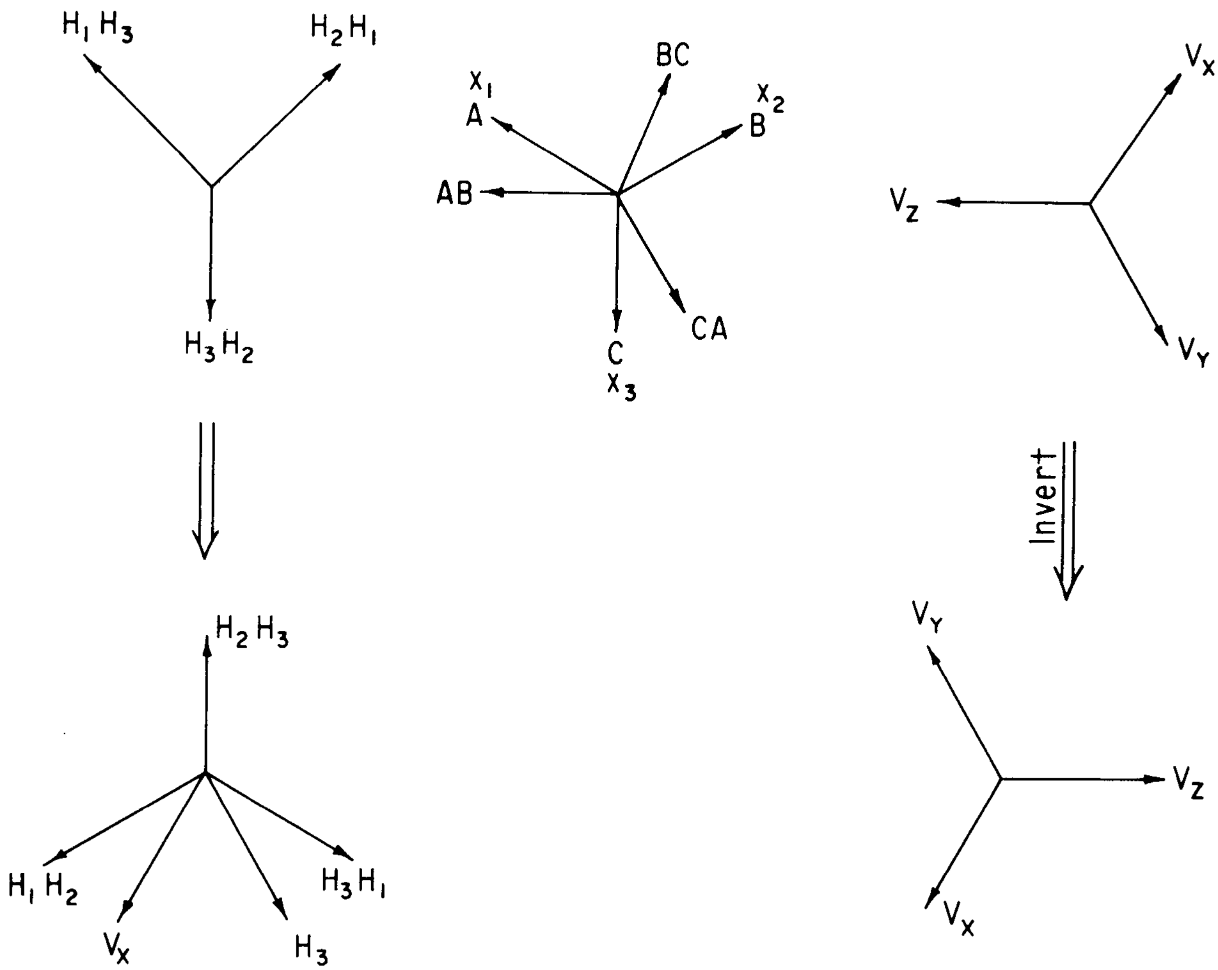
$$V \approx (60\text{A})(12.5 \text{ ohms}) \approx 750\text{V}$$

Therefore, using 12.5 ohms for the insertion resistor results in about 0.5 per unit voltage across the resistor. Of more importance, the startup inrush current is eliminated and the startup current is limited to 60 amperes.

CALCULATION SHEET NO. 6

Transformer Connections





Note: As can be seen from this vector check, H_3 leads X_3 by 30°, and

This matches the oscillograph record

V_X leads $H_1 H_2$ by 30°

V_X leads $H_2 H_3$ by 150°

GPO 847-404

Mission of the Bureau of Reclamation

The Bureau of Reclamation of the U.S. Department of the Interior is responsible for the development and conservation of the Nation's water resources in the Western United States.

The Bureau's original purpose "to provide for the reclamation of arid and semiarid lands in the West" today covers a wide range of interrelated functions. These include providing municipal and industrial water supplies; hydroelectric power generation; irrigation water for agriculture; water quality improvement; flood control; river navigation; river regulation and control; fish and wildlife enhancement; outdoor recreation; and research on water-related design, construction, materials, atmospheric management, and wind and solar power.

Bureau programs most frequently are the result of close cooperation with the U.S. Congress, other Federal agencies, States, local governments, academic institutions, water-user organizations, and other concerned groups.

A free pamphlet is available from the Bureau entitled "Publications for Sale." It describes some of the technical publications currently available, their cost, and how to order them. The pamphlet can be obtained upon request from the Bureau of Reclamation, Attn D-922, P O Box 25007, Denver Federal Center, Denver CO 80225-0007.