flkO SLC
qi(SAAt Ref -

SELF-EXCITED MICRO-HYDRO GENERATOR WITH VOLTAGE AND FREQUENCY CONTROL

Nigel P. A. Smith, B.Sc, M.Sc.

A thesis submitted in partial fulfilment of the requirements of the Council for National Academic Awards for the degree of Doctor of Philosophy

September 1992

Department of Electrical and Electronic Engineering,
Nottingham Polytechnic, Burton Street, Nottingham.

Collaborating Establishment: The Intermediate Technology Development Group, Rugby, Warwickshire.

This thesis is dedicated to my Grandmother
F.E. Gellender
who sadly did not live to see its completion

SELF-EXCITED MICRO-HYDRO GENERATOR WITH VOLTAGE AND FREQUENCY CONTROL

N.P.A. Smith

ABSTRACT

Standard induction motors can be used as generators by simply connecting capacitors across the terminals of the machine. Induction machines are widely available, very robust and can be as little as one third of the cost of synchronous generators. However, hitherto, the controllers developed for stand-alone induction generators have been more costly and complicated than for synchronous generators. As a result, the complete induction generator systems were no cheaper or simpler than the well proven synchronous generator systems. The aim of this work has been to develop and demonstrate a new control approach which reduces the cost and complexity of induction generator systems sufficiently to make them preferable for micro-hydro schemes.

A fundamental investigation examines the acceptable voltage and frequency limits for small rural electrification schemes. This shows that in all cases the supply frequency can be allowed to increase significantly above its rated value. A turbine-induction generator model is developed and proven by comparison with experimental results. This is used to show that, with fixed excitation and voltage control by means of a variable ballast load, acceptable frequency regulation can be achieved for load power factor variations from 1 to 0.8 lagging. This approach eliminates the need for the costly and complicated variable leading VAR source used with conventional control systems. The resulting control system is no more expensive or involved than controllers for synchronous generators.

A variable mark-space ratio approach to ballast control has been developed, as this has advantages over the conventional methods. Care has been taken to design a robust, low-cost controller and this has been fully tested on hydro sites in England. Engineers from developing countries have been trained to locally manufacture the controller and, to date, fifteen locally manufactured units have been installed in Nepal, Sri Lanka and Indonesia.

CONTENTS

CHAPTER 1

INTRODUCTION

1.1 THE DEVELOPMENT OF MICRO-HYDRO PLANTS

1.1.1 Background

With the advent of commercially produced electric generators in the 1880's many small water mills, in the more developed countries, were converted to electricity production. These were the first micro hydro-electric plants, the classification micro is generally accepted for systems up to 100 kW [1]. With the development of high voltage transmission, in the early part of the twentieth century, a shift occurred from small-scale plants serving local electricity markets to larger units feeding into grid systems.

In recent years there has been a resurgence of interest in micro hydro generation, particularly in the less developed countries. The most notable example is China where approximately 40,000 plants below 100 kW have been installed in the past 20 years [2,3]. The most suitable areas for the exploitation of micro hydro potential are those where there is no grid supply either present or planned. These are often hilly or mountainous areas which with their steep rivers are ideal for micro hydro projects.

The development of micro hydro resources in developing countries has been a slow process, because of the difficulty of coordinating many small separate projects and a lack of support from developed countries. To quote E.S. Daffern, from the World Bank [4]:

"........ it is difficult for a donor to get prestige from a power station that could fit into a bicycle shed."

However, there is an increasing trend now, amongst aid donors, towards providing funding and assistance for smaller scale, more sustainable projects. This gradual change, along with growing approval and support for such projects within many developing countries, has produced a more favourable climate for micro hydro projects.

1.1.2 Micro hydro installations

Due to the expense of dam construction most micro hydro schemes are of the 'run-of-river' type. A typical layout is shown in figure 1.1 [5]. Often, in an attempt to save

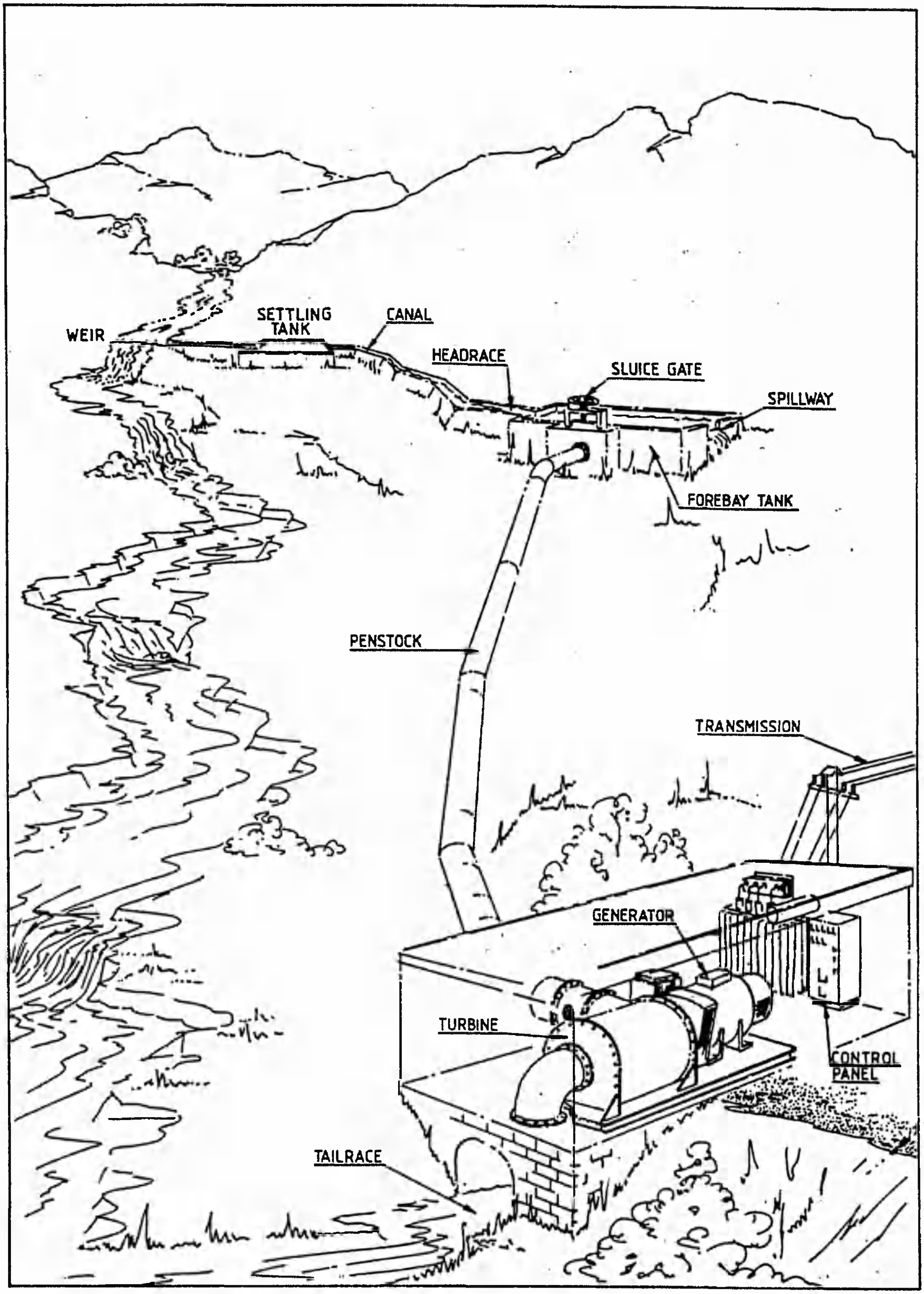


Figure 1.1 Main features of a run-of-river micro-hydro

money, smaller micro-hydro schemes are built without good intake structures or settling tanks. However, the increase in maintenance and down time that this causes generally outweighs the initial advantage.

A weir is normally used to divert the water from the river. In some cases it is possible to reduce costs by using natural features of the river to advantage. At the intake an overflow must be built so that excessive flows, especially flood flows, are returned to the river to prevent damage to the canal.

The water drawn from the river will contain some silt which can cause rapid wear in the turbine due to abrasion. To remove this material the water flow must be slowed in a settling tank so that the silt particles settle on the floor of the tank, where the deposit formed can be periodically flushed out.

The cheapest type of channel used to transport the water is an unlined earth channel. However, if the ground is very porous or unstable then a concrete or lined channel is required. The channel has a shallower gradient than the river, so that there is a net gain in potential energy.

The open channel ends at the forebay tank, which incorporates a spillway to return excess water to the river. When the sluice gate is opened, water enters the pressure pipe, known as the penstock, which delivers water to the turbine. The water is then returned to the river.

1.1.3 Comparison of micro hydro with other energy sources

The U.K. Department of Energy classifies wind, tidal, geothermal, wave and small-scale low head hydro as promising but uncertain sources of electrical power [6]. For heads above three metres small-scale hydro is categorised as being already economically attractive. Hydro schemes where the head of water available is only a few metres are generally more expensive per kilowatt than higher head installations since the canals, pipework and turbines have to be sized for higher flow rates. The Department of Energy places photovoltaics in a 'long shot' category. This would not necessarily be the case if the United Kingdom were a sunnier place.

The assessment of renewal energy sources by the Department of Energy is on the basis of grid-linked power plant. For stand-alone systems small-scale hydropower, unlike most of the other renewable power sources, has the advantage of generating a continuous and constant supply of electricity, provided sufficient water is available. Hydro power, particularly where medium or high heads are available, is generally the preferred alternative energy source in areas where suitable sites exist. According to the market research firm Frost and Sullivan [7], the world-wide installed capacity for remote hydro sites of below 1 MW was 9,995 MW in 1983, compared to 4 MW for photovoltaics and 11 MW for wind turbines.

The main alternative to micro hydro, in remote locations, is diesel generation. In 1983 the total installed capacity of stand-alone diesel sets was estimated to be 584 MW [7]. Diesel generation was favoured, when diesel oil was relatively cheap, because less capital was required. However, diesel generation costs in developing countries now range from 0.15 to 0.50 $/kWh, compared with an average cost of grid electricity of 0.07 $/kWh [8]. With the sharp and sustained increase in the price of oil during the 1970s and recent reductions in the cost of micro hydro equipment, water power has become more popular and in some areas is even replacing existing diesel sets. According to Frost and Sullivan [7], the average annual growth rate is 17% for stand-alone hydro and 6% for stand-alone diesel generation. On this basis there will be 48 GW of hydro plant and 1 GW of diesel plant in 1993.

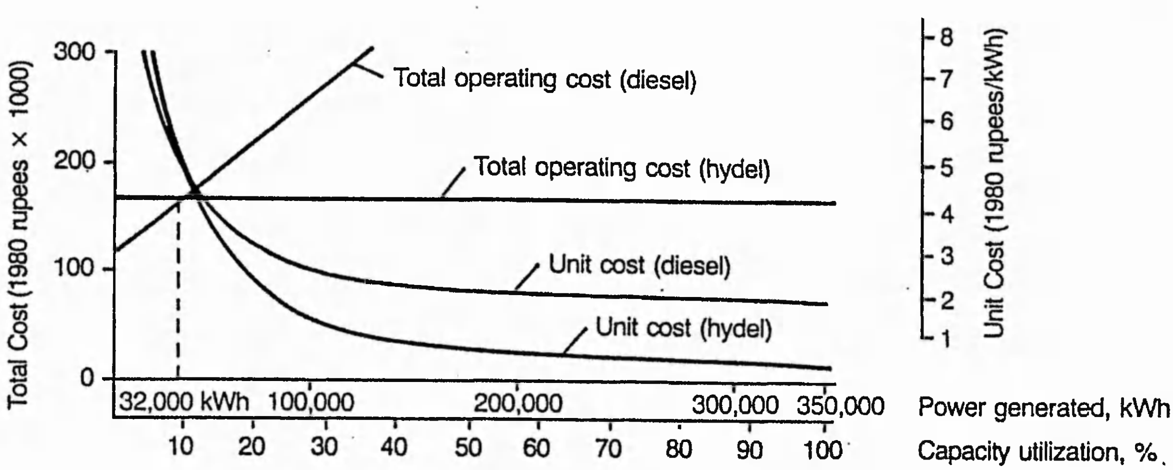


Figure 1.2 Cost comparison between 40 kW diesel and hydroelectric installations

The economic choice of which technology to use depends principally upon the load factor or capacity utilization of the plant. This is the average loading on the generator as a percentage of it's maximum power output. An example, from the work of Holland et al [9], is given in figure 1.2. In this example, hydropower is more economic than diesel if the load factor is greater than 10%. Cost reductions, achieved by using more appropriate turbines and control equipment, which, where possible, have been locally manufactured or assembled, reduce the load factor required for micro-hydro installations to be cheaper than diesel sets. In some developing countries, such as Nepal, the cost per kilowatt of micro hydro has become less than that of larger projects with their reliance on imported equipment and expertise [10].

Micro hydro installations are generally more reliable than diesel systems. According to Twidell [11], A modern diesel generator requires a major overhaul after every 10,000 hours of operation, which is equivalent to between two and three years for most sites. Fritz [12] states that:

"In the case of micro sets, up to 100 kW, diesel-generator useful life is often less than 10 years, especially under adverse conditions found in many rural areas."

In addition, poor availability of spare parts and diesel oil shortages can significantly increase the down-time of diesel generator sets, particularly in developing countries. By contrast, well designed micro hydro systems, particularly where induction generators are used, will last for tens of years of continuous operation with the sole maintenance requirement of routine regreasing and replacement of bearings.

1.2 MICRO HYDRO TECHNOLOGY

1.2.1 Water Turbines

This thesis is concerned with the control of generators coupled to water turbines. Whilst a detailed understanding of the theory and operation of hydraulic turbines is not required, knowledge of their power to speed characteristics is essential for the design of the electrical control system.

There are two classifications of turbine: impulse and reaction. Two examples of each are given in figure 1.3 [5]. With an impulse turbine, such as the Pelton or crossflow, the available head is converted to kinetic energy before striking the rotating part of the turbine, known as the runner or rotor. With such turbines the power conversion occurs at atmospheric pressure. In reaction turbines, such as the Francis or propellor, the runner is completely immersed in the flow of water and pressure is dropped across the runner. Impulse turbines tend to be used wherever possible, even if some gearing is required, as they are cheaper and easier to manufacture.

The following factors are taken into account when choosing a suitable turbine for a micro

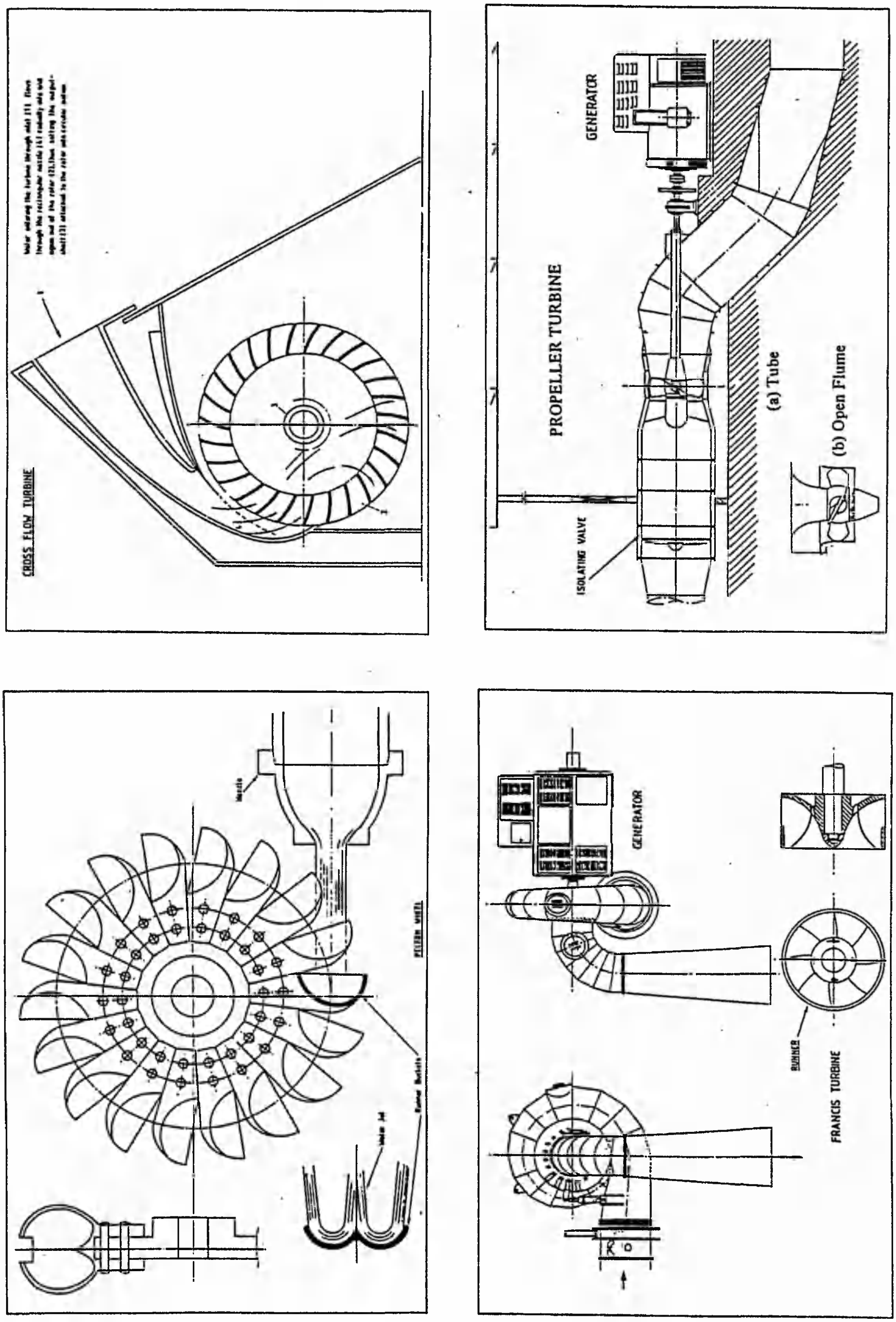


Figure 1.3 Four types of water turbine

hydro installation: Cost, reliability, efficiency, operating speed and runaway speed. The first three considerations are self-explanatory, for economic reasons efficiency is not normally as critical as cost, particularly when more water power is available than is required. The best operating speed for a turbine is generally its speed at maximum efficiency, though occasionaly turbines are operated into overspeed in order to reduce the runaway speed of the generator. Ideally the turbine operating speed should be the same as the speed required for the generator as such an arrangement does not require a belt drive, with its disadvantages of reduced efficiency increased cost and maintenance requirement. However, often speed increasing drives are necessary as water turbines tend to run at lower speed than generators. In order to aid selection of a turbine with a suitable operating speed for a given site, turbines are classified according to their specific speed, Ns, which is determined according to the turbine dimensions [13] and defined as follows:-

$$N_s = \frac{N\,P^{1/2}}{H^{5/4}} \qquad \text{.............. 1.2.1}$$

where N = turbine speed at best efficiency point (rev/min)
P = maximum turbine output power (hp)
H = net head (m)

To ascertain which turbine types are suitable for a proposed site the values of H, P, and N are substituted into equation 1.2.1 to determine the specific speed. Table 1.1 [14] can then be used to identify the turbine types that best meet the specific speed requirement. The values given in this table are the most useable specific speed ranges, in terms of cost and efficiency, for the more common types of turbine.

TURBINE TYPE	CLASSIFICATION	SPECIFIC SPEED
Pelton	Impulse	12 - 30
Crossflow	Impulse	20 - 80
Francis	Reaction	80 - 400
Propeller	Reaction	340 - 1000

Table 1.1 Turbine specific speeds

Turbine runaway speed, the speed reached when all external loads are removed, is an important consideration when choosing a turbine. For impulse turbines runaway speed is approximately 1.8 times the normal operating speed. Theoretically it is two times the normal operating speed, if friction and windage are ignored [15]. The theoretical and actual characteristics of an impulse turbine are shown in figure 1.4. For reaction turbines the overspeed ranges from less than 20% to more than 300%. Results by Kittredge [16] show that, in general, the higher the specific speed the greater the percentage overspeed.

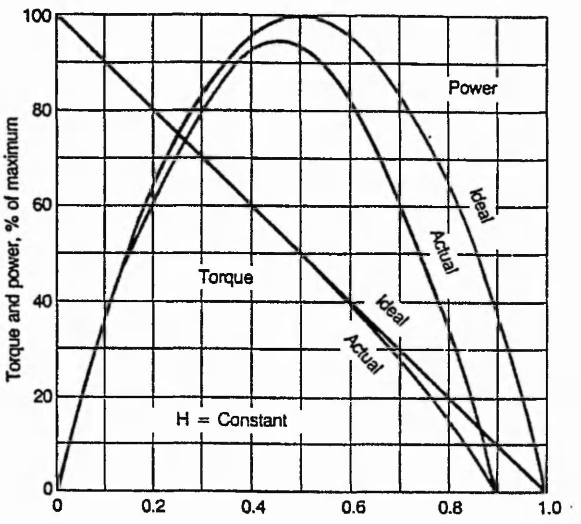


Figure 1.4 Torque and power against per unit maximum speed for an impulse turbine

Large overspeeds are dangerous for the generator, and in some cases the turbine, since the centripetal force on the runner will increase in proportion to the square of the speed. Deflectors and valves have been designed to prevent overspeeding, though these are expensive and can be unreliable unless regularly checked. In addition, because of the significant speed variation with load that occurs with most turbines, some form of control is generally required in order to regulate the output frequency of the generator.

For turbines with low percentage overspeeds little or no speed control is necessary to regulate the frequency of the electrical supply. In addition, overspeed protection is not required. Low specific speed pumps operated in reverse as turbines have this advantage, along with low-cost due to their mass production [16]. An investigation of the characteristics of such a pump was undertaken. The results are presented in chapter four.

1.2.2 Generator and associated control equipment

Except for very small micro hydro sets, of a few hundred watts, alternating current generators are invariably used. The main reasons for this are that a.c. generators are generally more reliable and most electrical goods are built for a.c. operation. Single-phase generation tends to be used for installations of 15kW and below, since phase balancing is unnecessary and the control and switch-gear required is simpler and cheaper than for a three-phase system. Three-phase generation is used for higher capacity installations and systems with long transmission lines or three-phase loads. At present synchronous generators are used for most stand-alone micro-hydro installations, principally because a voltage regulator is an integral and relatively straight-forward part of their design.

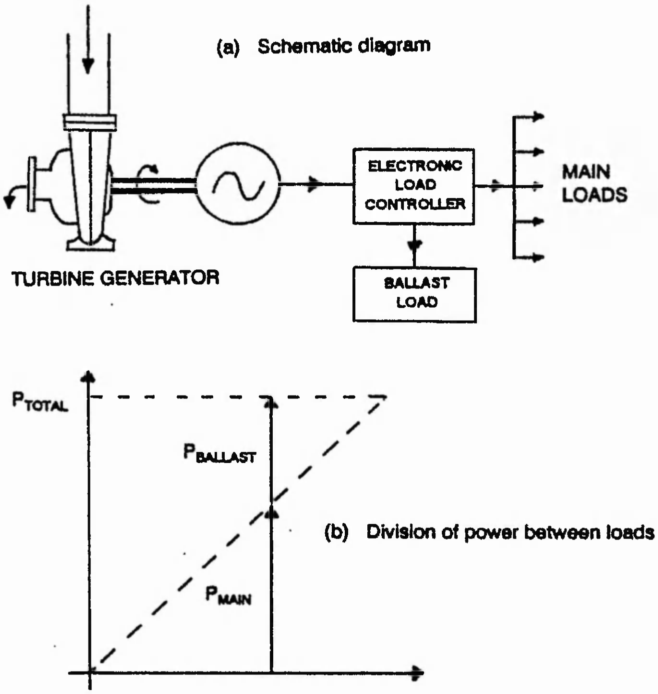


Figure 1.5 The basic principle of electronic load control

In addition to a voltage regulator, some form of frequency controller is generally required since the speed, and hence the frequency, of the generator will vary with load according to the characteristic of the turbine. This can take the form of a mechanical flow-controller, though these are slow acting and both complex and expensive. More recently electronic load-control has been introduced. This maintains a constant load, and therefore a constant frequency by dissipating a variable amount of power in a 'ballast' or 'dump' load. The basic principle of electronic load control is shown in figure 1.5. Well designed electronic load controllers have the advantages of high reliability, low-cost and fast response to frequency changes. A more detailed review of frequency control techniques is presented in section 2.2.

1.2.3 Breakdown of costs

The breakdown of costs for micro-hydro schemes varies significantly between installations in developed and developing countries. The greatest variation is in labour costs. In developed countries the cost of labour may be the most expensive factor in an installation, whereas in developing countries it can be almost insignificant. Low labour costs can reduce the expenditure required on equipment as well as construction. For example, the use of long feed canals can reduce the length, and hence cost, of the penstock pipe required. Holland [17] provides the following cost breakdown for a typical run-of-river installation in a developing country:-

Penstock	30%
Other civil work	20%
Mechanical and electrical equipment	40%
Engineering supervision	10%

In comparison, for large conventional schemes, incorporating water storage dams, the civil works generally represent 70-90% of the total cost [18,19].

The costs of the turbine and the generator, along with its control equipment, are significant in all installations. In developing countries they are generally the most costly items, because of the low costs of labour and hence civil work. A considerable amount of labour is required for small turbine manufacture, because the low demand and wide range of sizes required reduces the possibilities for mechanisation. In recent years local manufacture of turbines has begun in developing countries, including: Columbia, Indonesia, Nepal, Pakistan, Sri Lanka and Thailand. Hence turbine prices, in these countries have been reduced dramatically. Reports from Nepal and Pakistan [20,21] show that when local manufacture and local labour is used, to make the turbine and carry out the construction work, the generator becomes the most expensive part of the whole system. The results of the study in Nepal [20], shown in table 1.2, indicate how the addition of electrification to a hydro site used for agro-processing can more than double its costs.

In Nepal alone there are some 30,000 traditional water wheels in operation. Their efficiency is typically between 20% and 25% [22]. They are normally used for grinding flour and can mill in 15 minutes as much flour as can be ground manually in three days [23]. By replacing the traditional water wheel with the more efficient MPPU sufficient power is available to run a small generator to light the village in the evening [24].

Turbine	Traditional Water Wheel	MPPU**	Crossflow 1	Crossflow 2	Crossflow 3
Power (HP)	1	5	10	20	20
Grinder*	YES	YES	YES	YES	YES
Huller*	X	X	YES	YES	YES
Expeller*	X	X	X	YES	YES
12kW Generator	X	X	X	X	YES
Capital Costs					
1. Equipment	193	1476	2083	2648	10318
2. Construction	177	370	855	1917	1917
3. Transport	-	224	302	895	895
TOTAL	370	2070	3240	5460	13130
Annual Costs	55	145	160	275	395

*Types of agro-processing equipment

**Multi-purpose power unit - an improved version of the traditional water wheel.

Table 1.2 Cost of Nepalese hydro systems ($)

In the developed countries cost reductions have been achieved through technological advances and the use of new materials. Work being done using pumps operated in reverse as turbines, such as that by Williams et al [25] and Engeda and Rautenberg [26], indicates that considerable cost savings may be gained from this approach. However more research on the prediction of turbine characteristics from pump data is necessary.

1.2.4 Conclusions

Advances in micro-hydro are occurring as a result of technical developments and the appropriate application of technology. Developments have been taking place in turbine technology and generator control that both reduce costs and improve reliability. Also increased technology transfer to developing countries has resulted in further cost savings and better local repair facilities.

The cost savings resulting from these improvements now mean that the electric generator, is a more significant proportion of the overall cost of the installation. In developing countries the electrical system is often the most costly part of the system, a cost that must normally be met using foreign exchange. There is therefore a definite need for a more reliable, lower cost electrical generating unit.

1.3 COMPARISON OF SYNCHRONOUS AND INDUCTION GENERATORS

1.3.1 Introduction

The possibility of using squirrel cage induction machines as stand-alone generators was recognised by Basset and Potter in the 1930s [27] . Fixed values of capacitance enabled excitation to be achieved but loading, particularly with inductive loads, resulted in poor voltage regulation. With advancements in power-electronics, voltage control units to compensate for variable power factor loads have been produced. Such units in conjunction with an electronic load controller can produce a stable micro hydro generating system.

1.3.2 Cost

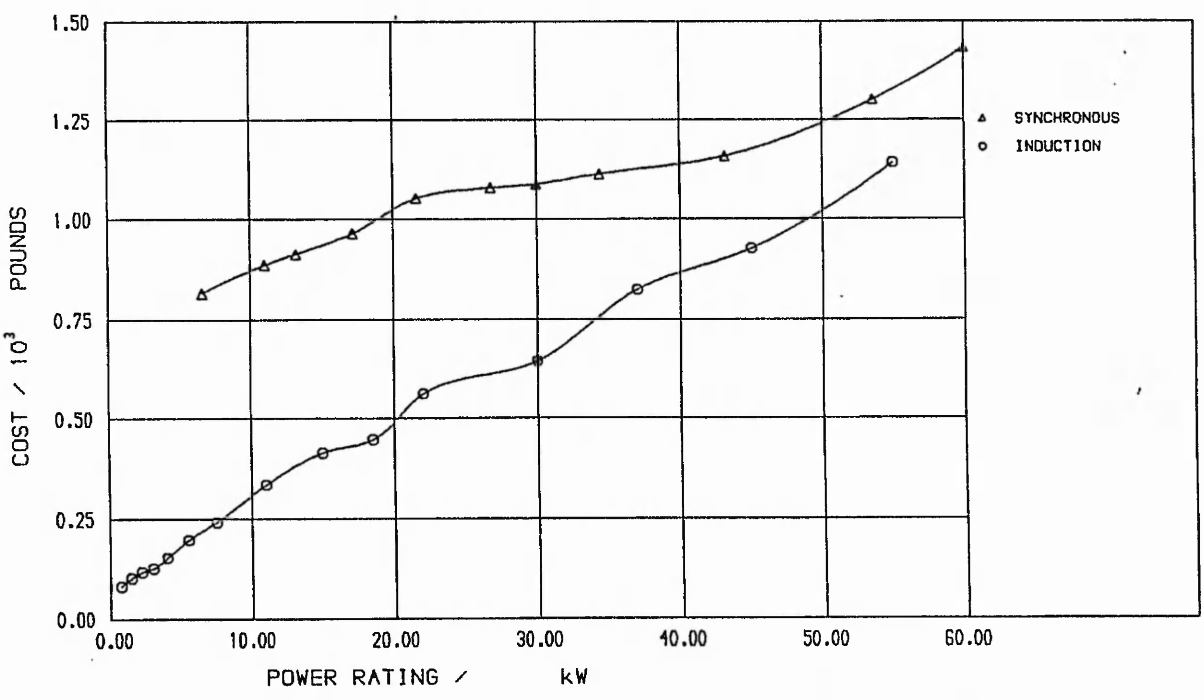


Figure 1.6 Machine cost against power rating for 4 pole, three phase, brushless synchronous generators and induction motors

Figure 1.6 indicates that for small sizes, up to 20 kW, cage induction machines are significantly cheaper than synchronous generators. The main reasons for this are that small cage induction machines are produced in much greater quantities than synchronous generators and they have a much simpler construction. A 6 kW induction machine, complete with it's excitation capacitors, costs approximately one third of the price of a synchronous generator of the same rating. With larger machines material costs become more significant, thus reducing the price advantage of induction machines. For machines of 50 kW and above induction machines may actually be more expensive, this is due in part to the fact that for the same output power less current flows in the stator and rotor of a synchronous machine and thus conductor costs are lower.

1.3.3 Reliability

Due to the isolated location of most micro hydro sets, reliability is of the utmost importance. This is particularly true in developing countries as there is invariably a shortage of engineers, especially in rural areas.

Induction machines are more rugged than synchronous generators for the following reasons:-

1) The cage rotors of induction machines are inherently more robust than wound rotors, since they are able to withstand considerably higher overspeed and overcurrent conditions.

2) The shafts of induction machines are built to sustain the substantial torque produced when started as a motor.

3) The machines are normally totally enclosed, preventing damage due to any water leaks from the turbine or penstock pipe.

If excitation of an induction machine is achieved by a fixed amount of capacitance, as in the methods proposed in this thesis, overloads will cause excitation to collapse thereby protecting the stator winding. Hence, such a generator is intrinsically fail safe. This is not the case with synchronous generators since the voltage regulators are designed to maintain the voltage irrespective of the load. Therefore, unless protection is built in or added externally, overloads can cause damage to the generator. Such protection systems, when implemented on small rural electrification schemes, are not ideal as it has been known for them to be bypassed if inconvenience is caused to the users. For example, at an installation in Nepal the problem of the repeated operation of a protection trip was 'solved' by placing a large stone upon the reset button! Under such circumstances a fail-safe generator would be a major advantage.

1.3.4. Voltage control

Synchronous generators are designed with built in voltage regulators. A relatively small current is varied in order to keep the voltage constant and external power-factor correction is not a necessity. In contrast, stand-alone induction generators require external control to supply the lagging VAR requirement of the generator and any inductive loads. The standard methods used to achieve this involve the control of large reactive currents and result in a more expensive and complex form of control than that used for a synchronous generator. A review of voltage controllers for self-excited induction generators is given in section 2.3.

1.3.5. Conclusions

Cage induction generators are considerably more reliable than synchronous generators. A conservatively rated machine should last for tens of years with only occasional bearing replacement. For sizes below 20 kW there are also significant cost savings to be gained by using induction machines. The main disadvantage of induction generators for stand-alone micro-hydro is the cost and complexity of voltage control. It is principally for this reason that induction generators have not been used for stand-alone systems to any significant extent. The aim of this research project was to overcome this drawback by producing a simple, reliable and low-cost controller for stand-alone induction generators driven by water turbines.

CHAPTER 2

THE CONTROL REQUIREMENTS FOR WATER DRIVEN STAND-ALONE INDUCTION GENERATORS

2.1 THE OPERATION OF CAGE INDUCTION MOTORS AS STAND-ALONE INDUCTION GENERATORS

2.1.1 Introduction

Grid-connected induction generators have been used for many years, their excitation current being provided by the supply. In a stand-alone situation the machine must self-excite. This requires the connection of capacitors across the terminals of the machine in order to provide the necessary magnetising current. A per phase equivalent circuit representation for a three-phase machine is shown in figure 2.1. In addition to requiring excitation capacitance, sufficient remanent magnetism must be present in the rotor for a build up of voltage to occur. The process of excitation is best modelled by considering the machine to behave first as a synchronous machine with a weak permanent magnet rotor and then as an asynchronous machine once a higher terminal voltage has been reached. Methods of analysis have been produced by Watson [28] and Elder et al [29].

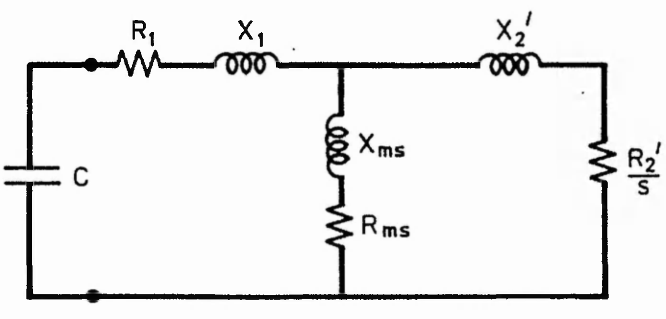


Figure 2.1 Per phase equivalent circuit for a self-excited induction generator

2.1.2 Capacitive requirements for self excitation

In order to operate an induction machine as a stand-alone generator a system is required that will provide the lagging VAR requirements of the magnetising and leakage reactances. This can be achieved by means of capacitors, connected across the terminals of the machine. The main reactive component of the induction machine, under normal voltage and frequency conditions, is the magnetising reactance. By considering the no-load condition, and neglecting the small slip and stator voltage drop, the per phase equivalent circuit of figure 2.2 can be used. The capacitor current I_c will equal the magnetising

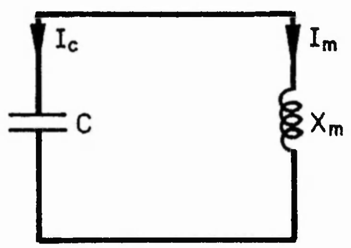


Figure 2.2 Simplified induction generator equivalent circuit at no load

current I_m and the machine and capacitor will act as a resonant circuit at angular frequency ω.

$$\frac{1}{\omega C} = \omega L_m \qquad \text{..............2.1.1}$$

Stable operation will occur provided that the impedance of the capacitors equals the magnetising reactance, as given by equation 2.1.1. The resonant frequency is fixed according to the machine speed. The only variable is the magnetising reactance, which is a non-linear function of current. Therefore, the excitation requirements, in terms of the per phase capacitance value required, are dependant upon the magnetising characteristic of the induction machine. The magnetising characteristic varies with the operating frequency and hence the shaft speed of the machine. Figure 2.3 illustrates this for a typical induction machine, and indicates that there is a threshold value of capacitance, for a given operating frequency, below which excitation will not occur.

A precise calculation of the capacitance required to generate a given voltage under specified load conditions is only possible with an accurate knowledge of the electrical parameters of the induction machine, including the variation of these parameters with

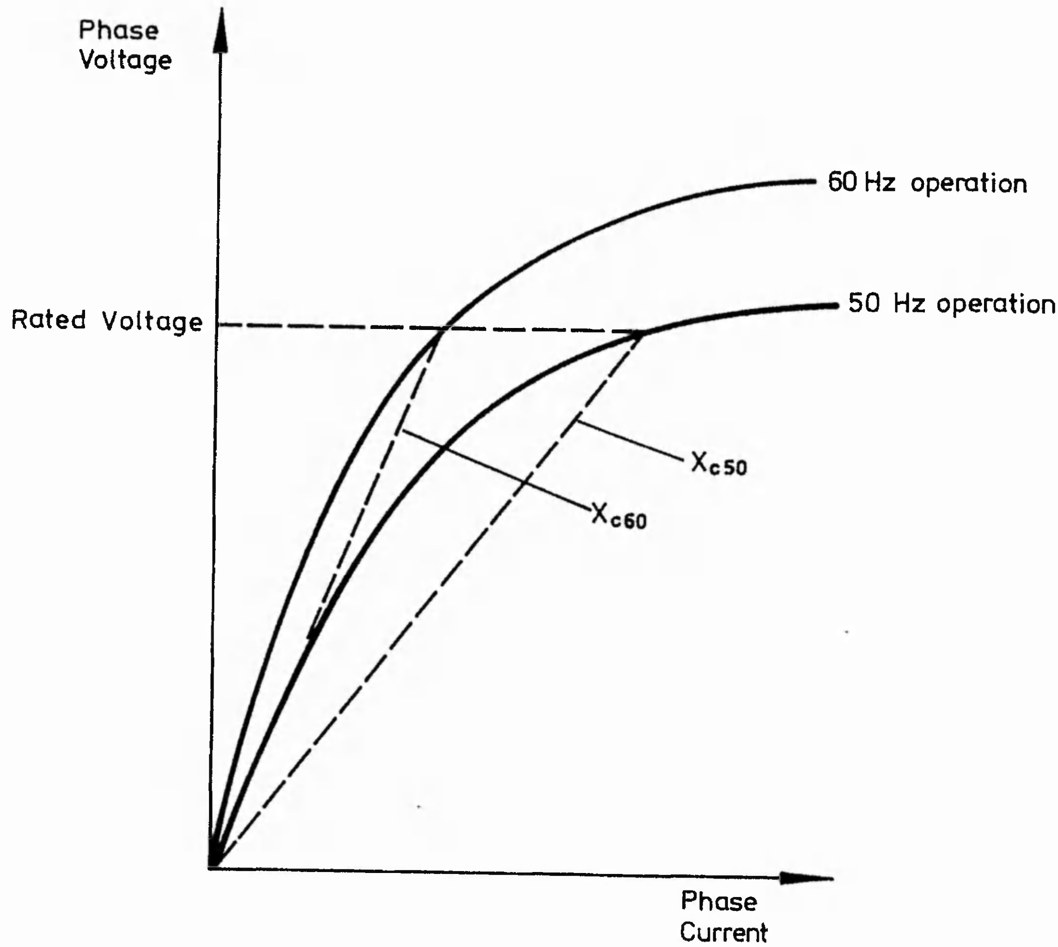


Figure 2.3 No load excitation characteristics for an induction generator at two frequencies

voltage [30,31]. The machine parameters can be obtained by standard tests, though this is time consuming and expensive equipment is required.

In practice it is sufficient to calculate an approximate value of excitation capacitance and vary either the turbine speed or capacitance until the required system voltage is obtained [32]. This approach was used for the the U.K. and overseas field trials described in section 5.4. An approximate value of the capacitance required can be estimated from the motor name plate or from manufacturer's data, by determining the capacitance required to fully correct for the lagging power factor of the motor when operated at full load.

2.1.3 Provision of remanent magnetism

In order for a machine to excite in a stand-alone situation a certain amount of remanent magnetism must be present to provide the initial magnetic flux. Sufficient magnetism is normally present, each time the machine is used, though, experience has shown that, this may not be the case if previously the machine suffered an impact, during transit or on site, or generation was collapsed with a resistive load connected. Remanent magnetism is also dependant upon the properties of the core material used.

Remanent magnetism can be increased by connecting a d.c. supply across the machine terminals. A car battery is generally more than sufficient for this purpose. Tests with machines of 2 kW and below have shown that two series connected 1.5 Volt, D-type, zinc-carbon batteries can produce adequate remanent magnetism for self-excitation to be achieved.

2.1.4 Rating considerations when using an induction motor as a generator

The maximum permissible continuous output of an induction machine when used as a generator is limited by the temperature of its windings. The winding temperature depends largely upon the stator current. Hence, a limiting factor, when considering the maximum electrical output as a generator, is that the stator current when generating must not be greater than the full load motor current. For a three phase induction machine, the mechanical power output as a motor and the electrical power output as a generator are as follows:-

$$P_{mot} = 3\,V\,I\,\eta\,pf \qquad \text{.............2.1.2}$$

$$P_{gen} = 3\,V\,I\,pf \qquad \text{.............2.1.3}$$

where P_{mot} is the mechanical power output as a motor
P_{gen} is the electrcial power output as a generator
η is the motor efficiency
pf is the power factor

For a machine whose current and power factor are the same when generating as when motoring a higher output can be achieved as a generator, as given in equation 2.1.4.

$$P_{gen} = \frac{P_{mot}}{\eta} \qquad \text{.............2.1.4}$$

In practice the increase will be smaller than that given in equation 2.1.4, because for the same stator voltage the power factor is lower in generating mode, as a result of increased saturation. In addition, rotor heating must be considered, since, especially for a totally enclosed machine, the stator core and winding may run hotter in generator mode. This effect will be reduced, at least in part, due to increased fan cooling, resulting from increased slip.

Based on manufacturer's data for motors of 1 to 50 kW, the maximum permissible continuous output of an induction machine when used as a generator is between 105% and 115% of the motor rating. Murthy et al [33] suggest that much higher outputs, up to 160% of the motor rating, are possible, however, they fail to take account of the power factor of the generator.

When an induction motor is operated as a generator consideration should also be given to how this effects the shaft torque and lifetime of the bearings. If the machine is operated as a generator with the same power rating as for a motor then the mechanical input power to the generator must be greater than the motor output power, in order to account for the electrical and mechanical losses. The difference in mechanical power depends on the generating mode efficiency, as shown in equation 2.1.5.

$$Pmech_{gen}\,\eta_g = Pmech_{mot} \qquad \text{..............2.1.5}$$

where $Pmech_{gen}$ is the shaft power in generating mode

$Pmech_{mot}$ is the shaft power in generating mode

η_g is the efficiency in generating mode

This in turn means that the shaft torque will be higher when generating than when motoring:-

$$T_{gen} = \frac{T_{mot}}{\eta_g}\,\frac{\omega_{mot}}{\omega_{gen}} \qquad \text{..............2.1.6}$$

where T_{gen} and T_{mot} are the torque in generating and motoring mode

ω_{gen} and ω_{mot} are the angular velocity in generating and motoring mode

However, this increase in torque is offset, in part, by the increased angular velocity in generating mode due to negative slip. Manufacturer's data for modern cage induction machines indicates that for machines of 1 kW and above the increase in shaft torque will be less than 15%.

Most induction machines are designed to allow direct-on-line starting. Under such conditions the starting torque will invariably be greater than the small increase required for generator operation at an output equal to the motor rating. Hence, the increased torque on the shaft is not a restriction when generator operation is considered. Bearing life is generally a more important consideration than shaft life, especially as machine useage is likely to be higher for generator operation than for motor operation. Increased shaft torque will certainly reduce bearing life, though the type of coupling between the turbine and generator is more significant. Bearing life will be much lower if gearing is used, due to the radial forces involved, than for a well aligned direct coupling.

In conclusion, when selecting an induction motor for use as a stand-alone generator the motor rating can be used in most cases. Operation at slightly above the motor rating is permissible, though not advisable if the machine is used continuously at full output.

2.1.5 Single phase induction generators

As explained in section 1.2.2, single-phase supplies tend to be used for installations with capacities below 15 kW. Single phase induction motors can be used for this purpose, but they are more expensive than three phase machines and are rarely available for sizes above 3 kW. A very satisfactory alternative is to use a three phase induction machine with excitation capacitors connected as shown in figure 2.4 [34]. The value of the capacitance

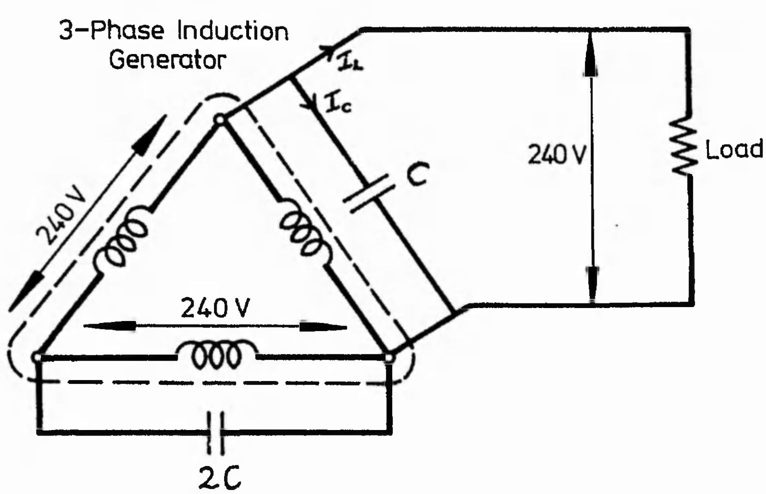


Figure 2.4 Single phase output from a three phase induction machine

"C" is the same as the per phase capacitance required for the same machine connected in delta and with a balanced three phase load. The arrangement for a single-phase loads has , for obvious reasons, become known as the "C-2C" connection.

$$I_L = \sqrt{3}\, I_C \qquad \text{..............2.1.7}$$

It can be shown that for the load condition given in equation 2.1.7 the generator behaves as a balanced three-phase machine. Either side of this condition the machine is unbalanced and runs hotter than a three phase machine with balanced load. As a result of this inbalance, the maximum output with the "C-2C" connection is typically 90% of the maximum output under balanced excitation and load conditions, for the same maximum winding temperature [35].

2.2 A REVIEW OF THE PRESENT FREQUENCY CONTROL TECHNIQUES AND THEIR SUITABILITY FOR USE WITH INDUCTION GENERATORS

2.2.1 Introduction

With large power systems frequency regulation is generally achieved by controlling the output power of generating sets that are connected to the grid and adding or removing generating sets as required. For small stand-alone systems using just a single generating set the same basic approach of 'supply follows demand' is generally used. It can be classified as a regulated output variable load (ROVL) approach. An alternative approach exists, that of constant output constant load (COCL) control.

The ROVL approach is used on most stand-alone systems since, by regulating the input power, according to the user load, energy is used efficiently. In general, the smaller the generating capacity the greater the percentage load changes that occur. Fast acting turbine governors are required in order to respond to these load changes. For small installations the governor represents a significant proportion of the total equipment cost [36].

COCL systems are suitable for installations where energy conservation is not required or is not possible. Run-of-river hydro systems are a good example of such installations, since if the water power is not harnessed then it will be dissipated by heating the water and air. A constant load is achieved by diverting excess capacity to a ballast load, as introduced in section 1.2.2. The COCL approach, if used on systems where energy costs are negligible or non-existant, has the advantage over ROVL systems of a faster response to load changes, since electrical load switching is much faster than mechanical governing. Also, for small installations, COCL control systems are much cheaper than ROVL governors.

Although not directly relevant to micro-hydro, it is worth noting that consideration, by Schweppe et al [37] and others, is being given to using the constant output constant load approach for large grid systems. Instead of dissipating excess capacity in ballast loads non-critical loads such as space heaters, water heaters and ovens can be rescheduled as required to reduce the requirements for spinning reserve, pumped storage systems and the extra capacity required to meet peak loads.

2.2.1 Mechanical governing

The traditional method of controlling micro-hydro plant involves mechanical governing either of the inlet valve to the turbine or, for some types of turbine, the guide vanes. This is a regulated output variable load approach. Governing is achieved by a flyball arrangement which senses turbine speed and either directly or, where larger forces are required, by hydraulic control adjusts the flow or vanes as required. A report by the Swiss centre for appropriate technology [38], describes in detail the design and operation of such a governor. Electro-mechanical governors are available. They use a tacho-generator or counter-timer to measure frequency and have motor driven controls. The main advantage of mechanical and electro-mechanical governors is that, since they control the flow rate, more efficient use of water resources will be achieved. However, this is only an advantage where water resources are limited and a storage dam is constructed. The disadvantages are that they are expensive, require regular maintenance and have a slow response time.

If used with stand-alone induction generators there will be small changes of frequency due to the variation of slip with load. By themselves these variations will not be significant, however they must be added to the speed regulation of the governor. For example, a mechanical governor installed on a 30 kW plant in Nepal, and described by Meier [39], had a speed variation from +10% at no-load to -6% at rated load.

2.2.2 Electronic load control

Electronic load control, introduced in section 1.2.2, is a constant output constant load approach. A constant frequency is maintained, when the main load on the generator changes, by controlling the amount of power dissipated in a variable ballast load. The ballast load generally takes the form of heating elements that either dissipate the excess power into the air or are used for water heating. The power dissipated in the ballast load is rarely used to the full, because it is not always required and is not controlled directly by the user.

There are two main types of load controller; one uses a variable firing delay-angle method, generally known as the phase-angle approach, and the other switches binary weighted loads. A paper by Henderson and Macpherson [40] introduces and contrasts both of these methods.

A) Phase-angle control

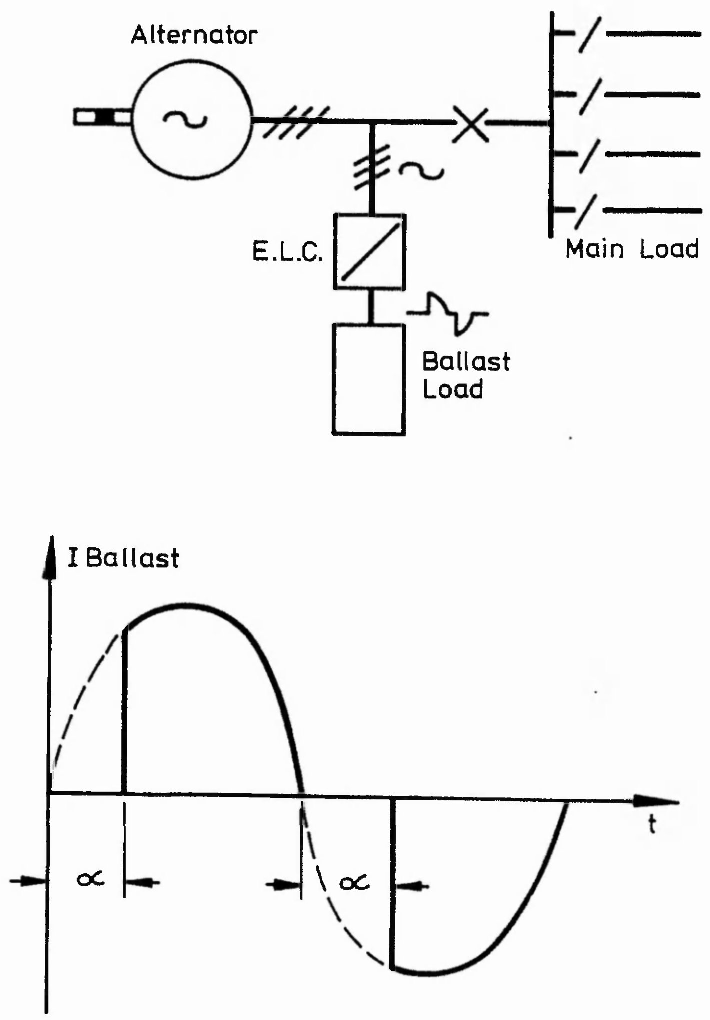


Figure 2.5 Phase-angle controller and ballast current waveform

A variable resistive load is produced by varying the firing angle of the triac or thyristor arrangement. A number of load controllers, that use this control method, have been developed, such as that designed by Pittet [41]. Figure 2.5 shows such an arrangement along with typical current waveforms. The production of a variable resistive load by phase-angle control also results in a variable lagging power-factor since the fundamental of the current waveform lags the voltage. Expressions for the real power, P, and reactive power, Q, as derived by Watson and Watson [42], are as follows:-

$$P = \frac{V_{rms}^2}{R\pi}\left(\pi - \alpha + \frac{\sin 2\alpha}{2}\right) \qquad \text{..............2.2.1}$$

$$Q = \frac{V_{rms}^2}{R\pi}\sqrt{\frac{1}{\pi}\left(\pi - \alpha + \frac{\sin 2\alpha}{2}\right) - \frac{1}{\pi^2}\left(\pi - \alpha + \frac{\sin 2\alpha}{2}\right)^2} \qquad \text{..............2.2.2}$$

where α is the delay angle
R is the ballast load resistance

Table 2.1 gives the values for P, Q and the power-factor for a range of delay angles.

Delay angle α (°)	Real Power P (Per-unit)	Reactive Power Q (Per-unit)	Power-Factor
0	1.0	0.0	1.0
30	0.971	0.167	0.986
60	0.804	0.397	0.897
90	0.5	0.5	0.707
120	0.196	0.397	0.443
150	0.029	0.167	0.171

Table 2.1 Variation of real and reactive power with delay angle for a phase-angle controller

Waveform distortion and a variable lagging power factor are the main disadvantages of the phase-angle controller. The waveform distortion has a similar effect on both synchronous and induction generators, though the excitation capacitors of an induction generator perform a beneficial filtering function. The non-sinusoidal current waveforms result in an increased heat dissipation in the machine windings. The heating effect is generally compensated for by oversizing the generator. Voltage distortion due to rapid turn on of the switching device will cause radio interference and voltage dips that may cause incorrect firing of other switching devices elsewhere in the system. Voltage dips are reduced by placing a coil in each of the ballast phases. A full site-test report of a phase-angle controller, tested with synchronous generators, has been produced by the Hangzhou Regional Centre for Small Hydropower, China [43].

The power factor variations due to changes in firing angle affect synchronous and induction generators differently. With synchronous generators the lagging power factor will result in an increase in stator current. This will exacerbate the problem of increased heat dissipation due to the non-sinusoidal current waveform and is in addition to the effect of any inductive consumer loads connected to the generator. With a stand-alone induction generator the lagging power factor will not cause increased stator current. This is because any inductive loads are power-factor corrected by the excitation capacitors. However, this reduces the amount of capacitance available for excitation and will therefore reduce the operating voltage of the generator. To maintain a constant voltage with an induction generator an additional and variable source of power factor correction must be added to the system.

The advantages of this form of load controller are very accurate frequency control and the requirement of just a single power electronic device and resistive load per phase. The use of just a single power electronic device per phase is only an advantage for relatively low power outputs since for larger capacities forced air cooling is required. This increases cost and complexity. Since resistive loads are rarely mass-produced for capacities above 6 kW, several loads in parallel will be required for larger systems.

B) Switched binary weighted loads

Load controllers using binary weighted load arrangements have been developed by a number of small firms and university research groups. The operation of such control systems is described in detail by Kormilo and Robinson, Elder et al and Spittal [44,45,46]. In each case the variable resistive load is achieved by switching in a combination of fixed resistors. The values of these fixed resistors are usually binary weighted so as to achieve the maximum number of load steps with the minimum number of resistors and switches. A single phase, three resistor arrangement is shown in figure 2.6, along with the seven values of ballast load that can be achieved.

The main advantage of this approach is that steady-state waveform distortion is negligible. Also, transient distortion will not be significant if load switching is performed near the

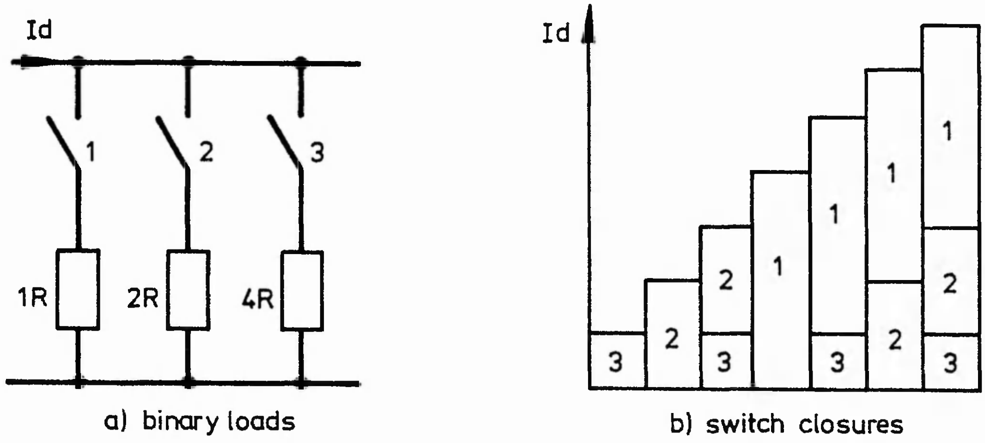


Figure 2.6 Binary-weighted load controller and ballast current range

zero-crossing point of the generated voltage and the ballast loads do not contain a significant inductive component. Hence the generator need not be oversized and controllable power factor correction for the ballast loads is not required.

The main disadvantage of the binary weighted controller is that it results in extra complexity in terms of the number of power devices and loads that are required. A compromise is made between the accuracy of frequency regulation and the cost and complexity of the controller. Depending upon the generated output and number of ballasts required, off the shelf loads, that are binary weighted in terms of resistance, may not be available. This results in either additional expense in terms of purpose-wound resistors or added complexity resulting from making do with combinations of non-ideal values.

2.2.4 Comparison of present frequency control approaches

The main criteria used when choosing micro-hydro equipment are cost and reliability. Mechanical governors are complex devices made using precision engineering equipment. They are therefore expensive even for small sizes, though the cost per kilowatt becomes appreciably less for larger units, due to economies of scale. A very low-cost mechanical governor being developed by Fischer [47] for the German agency for technical cooperation has the following specifications:-

(a) Maximum output 30 kW.
(b) Cost £1,000 (non profit local manufacture in a developing country).
(c) Labour 250 hours approx.
(d) Weight 215 kg.

In contrast an electronic load controller of the same capacity would be specified as follows:-

(a) Maximum output 30 kW.
(b) Cost £250 approx (non profit local assembly).
(c) Labour 20 hours.
(d) Weight 10 kg (including case).

These specifications clearly indicate the advantages of electronic load control. Further advantages are apparent when one considers the manufacture of such units. A well equipped workshop is required to build mechanical governors whereas the assembly of an electronic load controller can be achieved with a small number of hand-held tools.

A well designed load controller, due to the absence of moving parts and the high reliability of electronic components, will have a very long life. Highly trained maintenance technicians are not required since circuit boards can simply be replaced if they fail. By contrast, a mechanical governor, however well designed, will wear out in time due to it's large number of moving parts. In addition, the maintenance requirement of mechanical governors is much higher than for load controllers. In remote areas of developing countries technical expertise is almost non-existent, this can result in problems even with standard maintenance operations. A good example of this occured in Papua New Guinea and is reported by Robinson [48]:

"The grease nipples on the bearings of the Gilks turbine always showed signs of being freshly greased and the grease gun stood reassuringly in the corner. However, bearing noise increased alarmingly with each visit. Not until the bearings were close to destruction was it discovered that a small quantity of grease was regularly pumped from the gun onto a finger and then carefully placed on top of the grease nipple!"

Mechanical governors do not become the preferred speed control option until larger sizes, generally above 100 kW, are considered. Their advantages for these sizes are that their cost is a smaller proportion of the overall cost, their water saving benefit may be more significant and skilled operating staff can be afforded. They are rarely the preferred option for micro-hydro schemes.

The choice between the phase-angle and binary weighted load controllers is generally made on the basis of availability, price, reputation and repair service. If the choice is to be made by a technical assessment of the two approaches then for synchronous generators phase angle control is preferable for small machines due to its simplicity. However, for larger capacity systems, binary weighted load controllers are the preferred option since a better waveform is produced and an oversized generator need not be used. The increased number of power devices and resistors is less of a drawback with larger binary weighted controllers due to the problems of cooling single high current power devices and the poor availability of resistive loads above 6 kW.

With stand-alone induction generators the power factor of the ballast load is very important. Binary weighted load controllers produce a variable unity power-factor load whereas with phase-angle controllers the power-factor produced by the ballast load varies according to the delay angle. If a phase-angle controller is used with an induction generator a method of controllable power factor correction is required to compensate for the variable lagging power factor effect of the ballast load.

2.2.5 Conclusions

For micro-hydro systems electronic load control has many advantages over mechanical control methods. The main advantages are lower cost, faster response, greater accuracy and better reliability. For these reasons electronic load control is employed on most new micro-hydro installations.

The choice between the two electronic control approaches depends principally upon the generator type and plant size. For small synchronous generators phase-angle controllers are generally to be preferred. Whereas, for larger synchronous generators and all induction generator installations binary-weighted controllers are most suitable.

2.3 A REVIEW OF THE PRESENT VOLTAGE CONTROL TECHNIQUES FOR INDUCTION GENERATORS

2.3.1. Introduction

For the reasons given in section 2.2, most modern micro-hydro system uses an electronic load controller to keep the generated frequency constant by maintaining a near constant resistive load on the generator. If a stand-alone induction generator is used then fixed values of excitation capacitance are sufficient to maintain a near constant voltage provided that the power factor produced by both the ballast and consumer loads is unity. In practice the load is rarely purely resistive, hence a source of variable leading VARs is required to correct for variations in load power factor. Voltage regulators for stand-alone induction generators are essentially power-factor correction devices. Only a summary of each of the common control techniques is given, because their cost and complexity renders them unsuitable for most micro-hydro systems.

2.3.2. Switched capacitor systems

The most straightforward method of power-factor correction, at least conceptually, uses switched capacitors. These are often binary-weighted in order to obtain maximum voltage regulation for the minimum number of switches. The approach is essentially the same as with the binary-weighted load controller, except that in this case the supply voltage is regulated by the controlled switching of capacitors. Careful timing of the switching operations is necessary in order to minimise inrush currents. A suitable arrangement is shown in figure 2.7. The capacitors are charged through the diodes. By triggering the

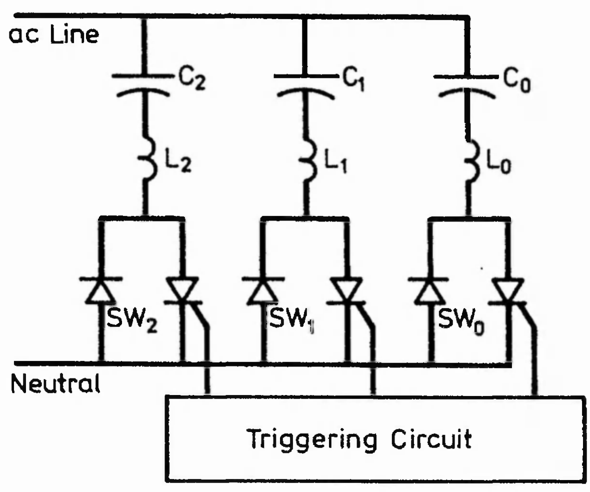


Figure 2.7 Switched capacitor system (one phase)

SCR at the negative peak of the phase voltage, and thus the zero crossing of the capacitor current, inrush will be limited. Small inductors are present in case the turn-on occurs with some voltage across the SCR. The operation of such controllers is explained in detail by Watson and Watson, El-Sharkawi et al, Elder et al and Bernays [42,49,50,51].

2.3.3 Fixed capacitance with phase-angle controlled inductor

This approach uses a fixed capacitor in parallel with a thyristor controlled variable inductor, connected across each phase of the induction machine, as shown in figure 2.8.

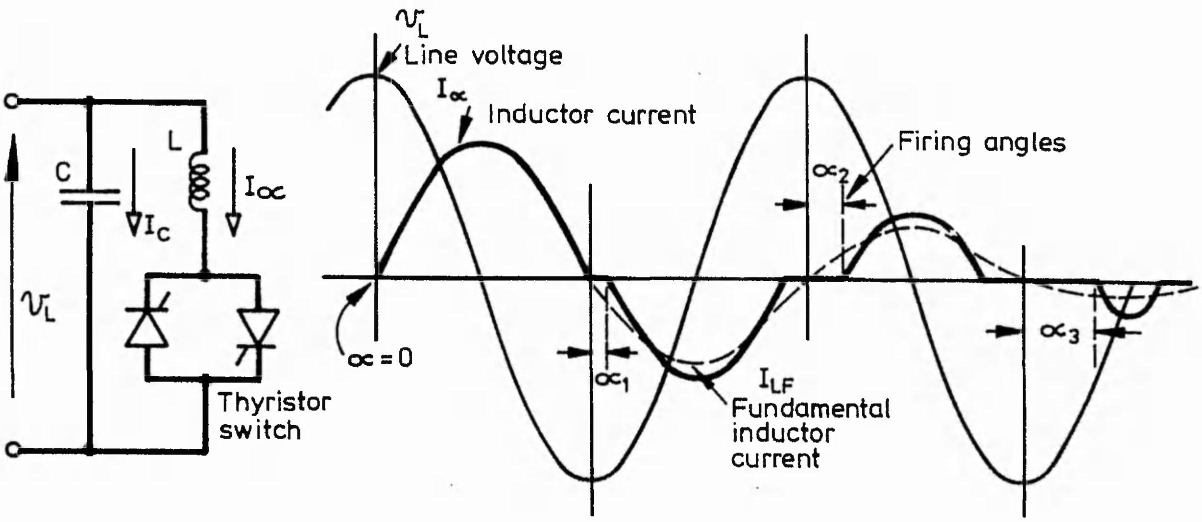


Figure 2.8 Fixed capacitance with phase-angle controlled inductor (one phase)

By varying the firing angle (α) of the thyristor switch the lagging current into the inductor can be controlled. The current into the inductor is controlled so that it is a maximum when the load on the generator is purely resistive and is reduced to compensate for inductive loads.

The fixed capacitor is selected so as to provide the maximum reactive power requirement of the generator and the load. The maximum inductor current must be at least equal to the maximum reactive current required by the load. This method achieves better voltage regulation than with discrete capacitor switching, though harmonics are introduced. Further details are given by Brennen and Abbondanti, Hammad and Mathur, Doradla and Patel, Iribarnegaray et al and Irisa et al [52,53,54,55,56].

2.3.4 Fixed capacitance with inductively loaded AC to DC converter

An inductively loaded AC to DC converter can be controlled using natural commutation to produce a variable inductive load or by forced commutation to produce a variable capacitive load. The basic configurations are given in figures 2.9 and 2.10. With the naturally commutated converter the fixed capacitance must be equal to the maximum required reactive power of the generator and load, as in the case of the phase-angle controlled inductor. Whereas with the forced commutated arrangement the fixed capacitance need only be sufficient to excite the generator.

Typical waveforms for the naturally commutated converter are shown in figure 2.11. With

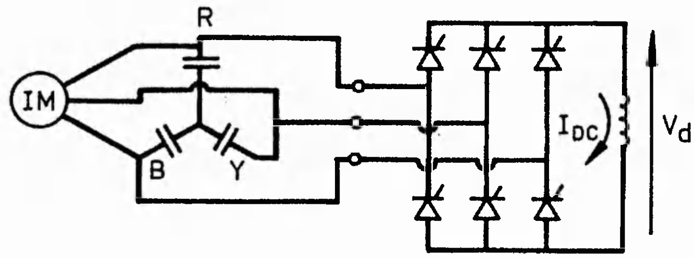


Figure 2.9 Basic configuration of a naturally commutated converter

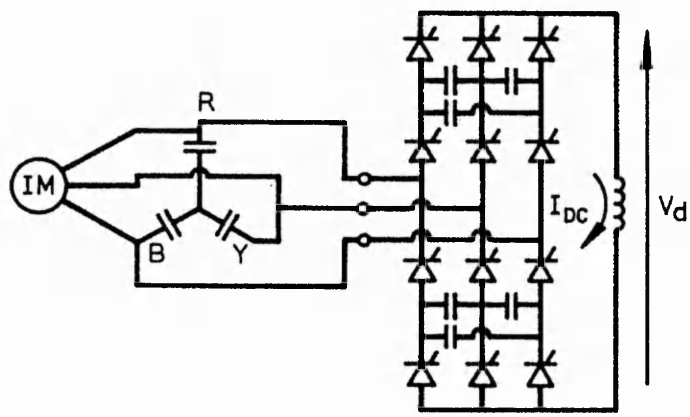


Figure 2.10 Basic configuration of a forced commutated converter

a sufficiently high inductance on the d.c. side of the converter, the a.c. line current drawn by the converter consists of pulses of near constant magnitude. The a.c. current is ideally displaced by 90° with respect to the current for an uncontrolled bridge, giving a fundamental component that is purely inductive. In practice the converter normally has a firing delay angle (α) of just less less than 90°. This yields a d.c. voltage sufficient to overcome the thyristor voltage drops and drive the required current through the inductor. By varying the firing angle, the inductor current, and hence the a.c. line currents, can be controlled so as to maintain a constant voltage for variations in load power factor.

The waveforms of the line currents indicate the presence of significant harmonics. However, the filtering action of the excitation capacitors reduces distortion of the generator and load waveforms. Naturally commutated converters are described in detail by Malik et al and Boys et al [57,58].

The forced commutated converter has the advantage over the naturally commutated converter of being a source of variable leading VARs. A firing advance angle of almost

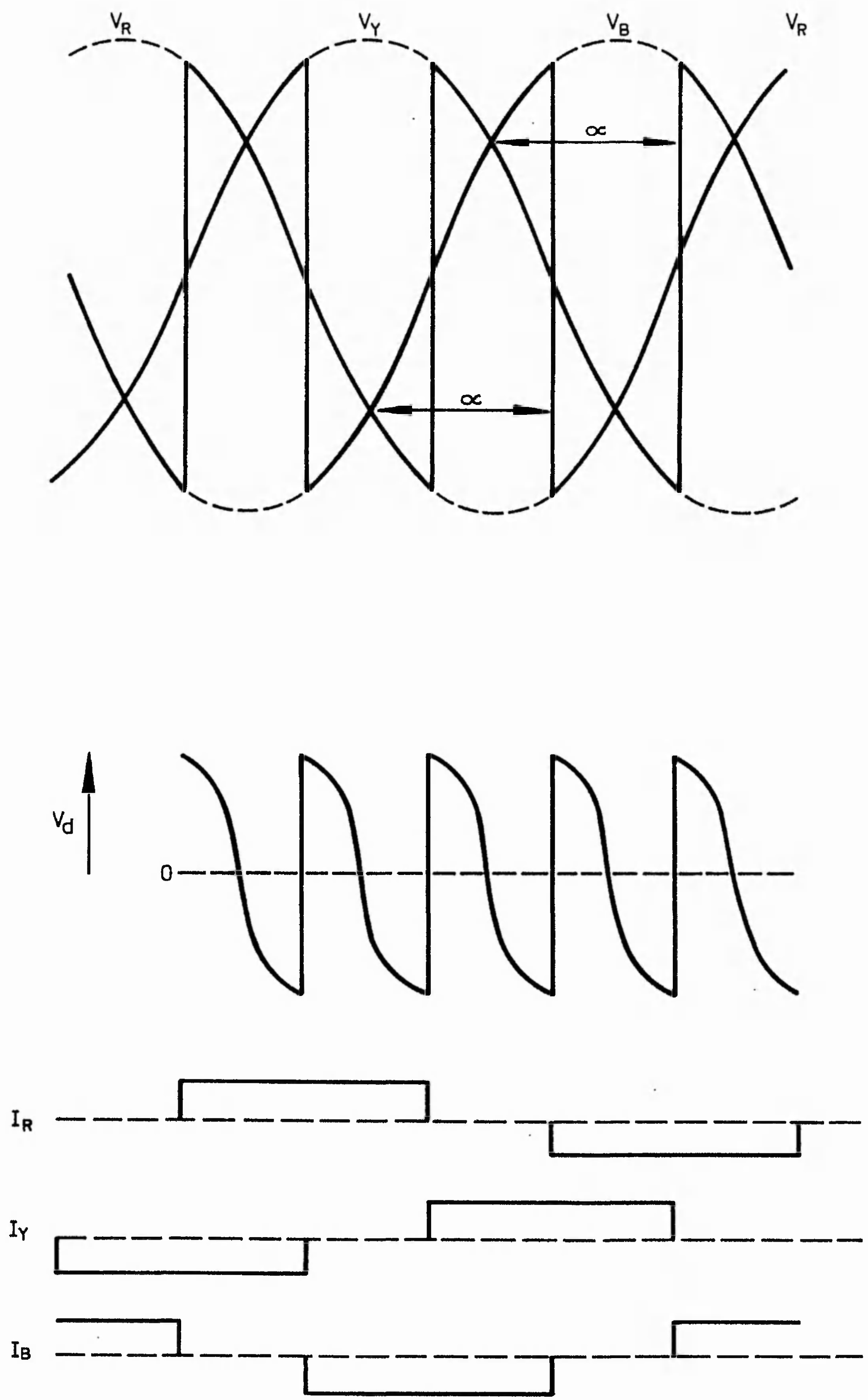


Figure 2.11 Voltage and current waveforms for a naturally commutated converter with delay angle of 90°

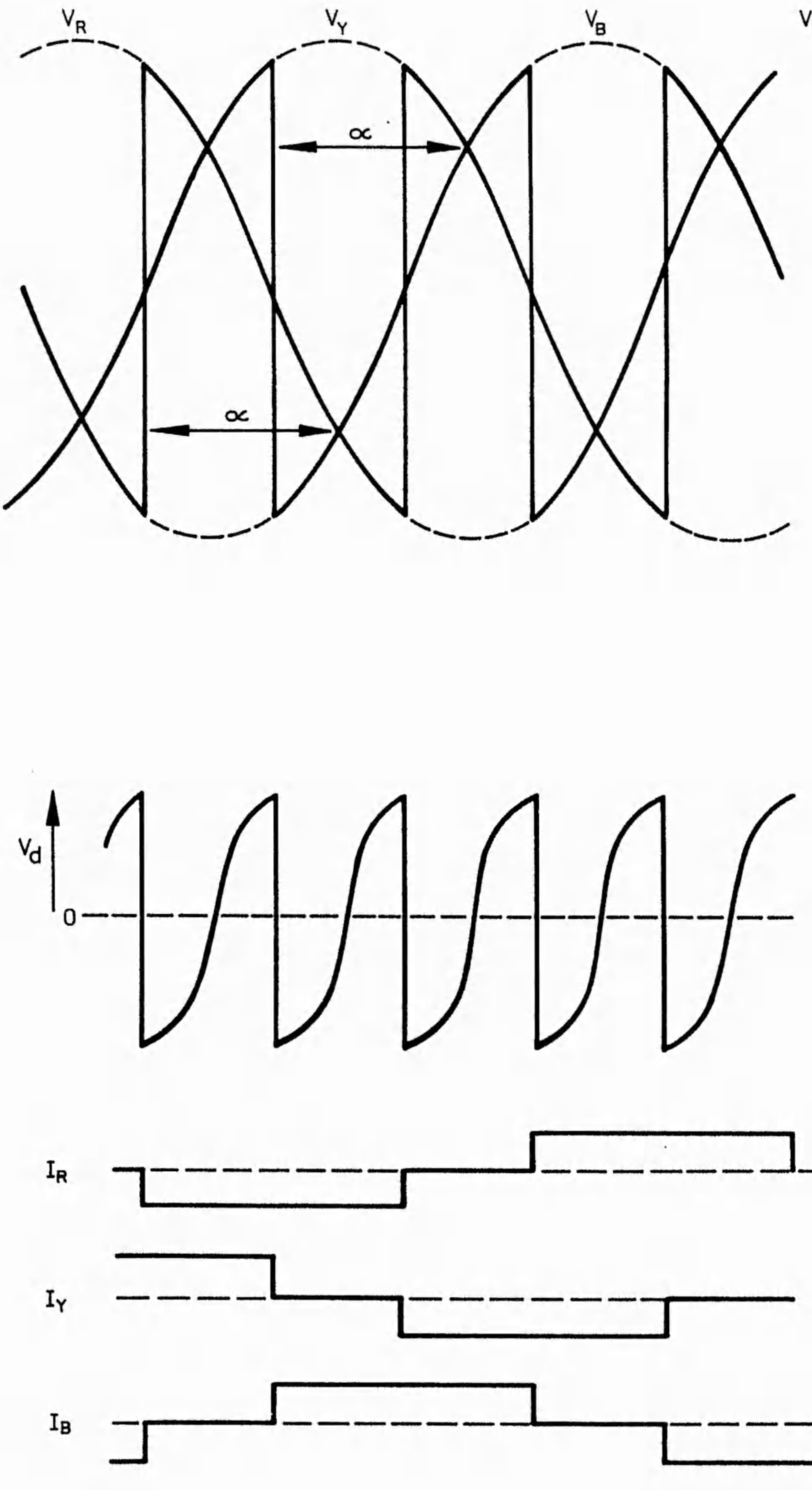


Figure 2.12 Voltage and current waveforms for a forced commutated converter with advance angle of 90°

90° is used as shown in figure 2.12. The disadvantage of the forced commutated converter is that additional power electronic devices are required to achieve commutation. Control systems for forced commutated converters are described by Epstein et al and Barreto and Watanabe [59,60].

2.3.5 Conclusions

Each of the four techniques that have been presented can be used to control the voltage of a self-excited induction generator. However each technique involves appreciable expense in terms of power-electronic devices, reactive loads and control electronics. In addition, the complexity of each of these types of controller will reduce the reliability of the overall system. Both of these factors must be taken into account when comparing induction generators with synchronous generators for stand-alone micro hydro systems.

The voltage controller for the induction generator performs the function of the AVR on a synchronous generator. In addition, for a standard micro-hydro system, an electronic load controller is also required. Hence, for an induction generator separate voltage and frequency controllers are required. This is in contrast to the synchronous generator approach, for which just a frequency controller is required since the AVR is an integral part of the generator.

Woodward [61] has produced a cost comparison of several approaches to the electrical components of micro-hydro systems. Updating his figures to 1992 prices, the costs for synchronous and induction generator units of 10 kW are:

SYNCHRONOUS:

Brushless synchronous generator	£ 900
Electronic load controller	£ 300
TOTAL	£ 1200

INDUCTION:

Induction machine	£ 300
Electronic load controller	£ 300
Voltage controller	£ 600
TOTAL	£ 1200

These prices indicate that there is no significant cost advantage to be gained by using induction generators, even at low output capacities for which the induction machine cost advantage is most appreciable.

In a recent review of micro-hydro research, Giddens [62] states that:

"The induction motor is cheap and rugged but the need remains for an electrical package that will control the excitation and voltage regulation in the generating mode."

He presented this viewpoint in 1986, some time after the papers on the voltage controllers reviewed in this section were published. His comments reinforce the view that although voltage controllers for stand alone induction generators exist none are suitable for wide-spread commercial application.

A new control approach is required if induction generators are to replace synchronous generators on stand-alone micro-hydro systems. The approaches taken in this thesis exploit the intrinsic characteristics of the induction generator and water turbine in order to reduce the control requirements and costs. These approaches are outlined in section 2.5 and further developed in later chapters. All the approaches considered result in a reduction in voltage or frequency regulation or both. Hence, the limits of acceptable frequency regulation must be determined.

2.4 THE SPECIFICATION OF AN ISOLATED ELECTRICITY SUPPLY

2.4.1 Introduction

The cost of the equipment required to maintain good voltage and frequency regulation, as a proportion of overall costs, is generally much higher for small generating systems. This is certainly true for micro hydro-electric systems, since they are usually built to similar voltage and frequency specifications to those of grid-systems. In some developing countries, where the grid supply at times operates outside its specification, stand-alone systems often produce a better regulated output than the grid. In many cases this stand-alone regulation is more than sufficient for the loads that are supplied. If the cost and degree of complexity of the control equipment concerned are similar to that of a system with just adequate regulation then it is obviously advantageous. If not then it is likely to be inappropriate, particularly for developing countries, as cost and reliability are generally the most important criteria. The control strategies presented in this thesis have been developed for practicality, in terms of cost, reliability and ease of implementation, rather than optimum regulation.

Before designing control systems that are simpler than those used at present, it is necessary to consider what degree of voltage and frequency regulation are acceptable. These will vary, not only according to the types of loads on the system but also, to some extent, upon the expectations of the user. For example, it is likely that someone who has never had the benefit of electric lighting will be prepared to put up with a greater degree of variation in brightness than a person used to a well regulated electricity supply. Ideally, except where an installation is put in for one specific use, the control system should be suitable for all common load types even if there are no plans to use them initially.

2.4.2 Voltage regulation

Voltages above the manufacturers ratings should be avoided as the lifetime of the load, be it a heater, lamp or motor will be reduced. Manufacturers' data indicates that for a standard tungsten filament lamp a continuous overvoltage of just 5% will decrease the expected life of the lamp by approximately 50% [63]. It is clear from this information that overvoltages must be limited as much as possible.

The effect of an undervoltage with a heater or incandescent lamp is to increase the life and decrease the output. In the case of fluorescent lamps too low a voltage can prevent turn on and may cause the starter to fail due to repetitive operation. With motors, the starting torque will be reduced and overheating may occur if the motor is fully loaded. Overheating may also occur with transformers, depending upon the load and the transformer design. With most power supply transformers undervoltages will not be damaging as the maximum load current will fall as the voltage falls. In the case of both motors and transformers lower magnetisation current at reduced voltage, as shown in figure 2.3, will help to offset the effect of any overloading.

British electricity regulations prescribe that the supply voltage must be maintained within +/- 6% of it's rated value. Most other countries adopt a similar standard. By maintaining the voltage within these limits electrical loads will be operated under their rated conditions and should therefore last for their rated life. For most micro hydro applications the voltage should be maintained within this range. Where a basic system for just resistive loads is installed the lower voltage limit can be extended without danger of damage to the loads, however output variation will be increased. For a voltage variation between +6% and -6% the heat output of a resitive load, ignoring resistance variation with temperature, will be 27% greater at the maximum voltage, relative to the output at the minimum voltage. For a tungsten filament lamp the variation in light output will be approximately 50% over the same voltage range [63]. Extending the lower voltage limit will increase the output variation irrespective of the load type. The effect upon lighting loads is the most significant since the output variation is greatest and may cause discomfort. If the lower voltage limit is extended to -15% the light output variation will be approximately 110% increasing to 170% for a -20% lower limit [63]. A system of voltage regulation within +6% and -15% is proposed here as a compromise between increased voltage range and reduced performance.

2.4.3. Frequency regulation

The operation of some domestic electrical appliances, such as television sets, record players and clocks, used, in part, to be dependant upon their synchronisation to the supply frequency and they therefore required a well regulated supply. This is not the case with more modern equipment since an internally generated reference frequency is used. Nowadays, the limits of acceptable frequency variation are principally set by the requirements of transformers and motors.

With motors, variations in performance due to changes in the supply frequency will differ according to the type of motor considered. The performance of universal motors, is least affected by such variations since their shaft speed does not depend primarily upon the supply frequency. Of the common types of motor, the type that is most affected by frequency variations is the induction motor. This is because its shaft speed is directly related to the supply frequency and its stator current will be affected by the variations in magnetising current with frequency. In the following discussion only induction motors will be considered since, of the common motor types, it exhibits the greatest variations in performance with frequency.

Heating effects with motors and transformers

For operation at a constant voltage, a reduction in frequency results in an increase in magnetising current. This causes an increase in the power dissipated in the primary winding of the transformer or the stator winding of the induction motor and, in both cases, an increase in core loss. These increases are very significant for modern equipment since a high degree of saturation tends to be used. An additional disadvantage of reduced frequency operation for motors is that the reduced shaft speed will result in a reduction in fan cooling. For these reasons operation of transformers and motors at below rated frequency should be avoided.

Motor starting torque

The maximum allowable supply frequency depends upon the characteristics of any motor loads that are present. For induction motor loads requiring a large starting torque, operation at above rated frequency will be detrimental to performance, or if the frequency is very high, may prevent the motor from starting. A good indication of the variation of torque with frequency can be obtained using the approximate equivalent circuit shown in figure 2.13.

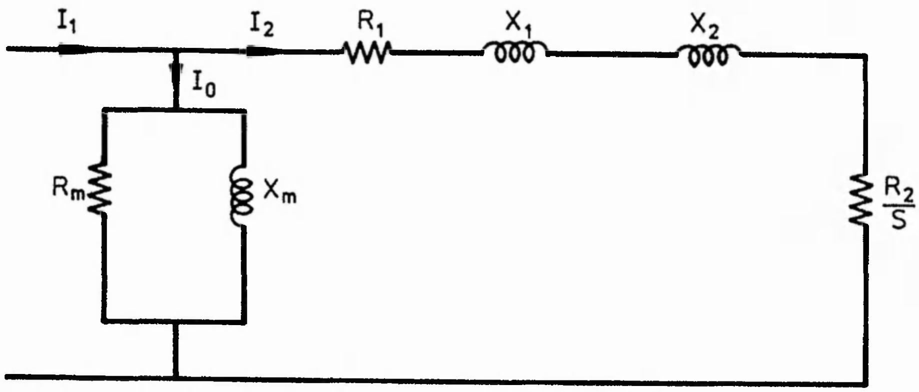


Figure 2.13 Per phase approximate equivalent circuit for a three phase induction motor

$$\text{Torque} = \frac{3\, I_2^2\, R_2}{\omega_s\, s} \qquad \text{.............. 2.4.1}$$

where ω_s is the angular velocity at synchronous speed

$$I_2 = \frac{V}{\left(\frac{R_2}{s} + R_1\right) + j(X_1 + X_2)} \qquad \text{.............. 2.4.2}$$

$$\text{Hence Torque} = \frac{3\, V^2\, R_2}{\omega_s\, s\left[\left(\frac{R_2}{s} + R_1\right)^2 + (X_1 + X_2)^2\right]} \qquad \text{.............. 2.4.3}$$

The variation of starting torque with frequency depends upon the ratio of the leakage reactances to the combined stator and rotor resistance. From equation 2.4.3, it can be seen that where leakage reactances dominate, torque decreases with increasing supply frequency with approximately a cube law relationship. This represents the worst case. In practice the rotor and stator resistances are a significant portion of the motor impedance and, due to skin effect, the rotor resistance will be higher at starting.

Motors are always conservatively rated since the drop in supply voltage upon starting, particularly with a 'weak supply', can cause a severe torque reduction. For a motor of a size that is significant in comparison with the generator rating and positioned at the end of a long transmission line the reduction in starting torque due to the drop in voltage can be considerably greater than that due to a 5% or 10% rise in frequency. If such frequency increases are present in a supply system care may be required, in terms of motor positioning, starting method and the choice of transmission line conductor rating, in order to prevent the combined effect, upon starting torque, of a voltage drop at above rated frequency.

Motor operating torque

The effects of variations in frequency upon the maximum motor torque must also be considered. The expression for the maximum torque of an induction motor, again on the basis of the approximate equivalent circuit, is as follows [64]:-

$$T_{max} = \frac{3\,V^2}{2\,\omega_s\left(R_1 + \sqrt{R_1^2 + (X_1 + X_2)^2}\right)} \qquad \text{..............2.4.4}$$

The percentage reduction in maximum motor torque with speed is less than that for starting torque. From equation 2.4.4, it can be seen that the worst case is a reduction in maximum torque proportional to the square of the supply frequency. Since the maximum motor torque is generally more than twice the rated torque the effect of a 5 or 10% frequency increase will not be significant.

Frequency dependant loads

A further constraint, to operation above rated frequency, occurs when motor loads whose power requirements increase significantly with speed, such as fans, are to be used on the supply. With universal motors the problem does not arise since frequency variations, of the magnitude considered here, have only very slight effects on the speed of the load. For induction motors the effect can be appreciable since shaft speed increases approximately linearly with frequency and the power requirement of fans increases at nearly the cube of the speed. The effect of this upon machine operating temperature was determined experimentally for a 2.2 kW induction motor. The experimental arrangement is described in Appendix A, and the results are presented in table 2.2.

FREQUENCY (Hz)	LINE CURRENT (Amps)	MOTOR LOAD (Watts)	TEMPERATURE RISE (°C)
50.0	8.23	2200	60.2
52.5	8.37	2547	63.0
55.0	9.17	2928	79.4

Table 2.2 Variable frequency supply results for an induction motor with load proportional to the frequency cubed

The temperature rises for the two tests at increased frequency are less than would have occurred for 50 Hz operation if the same, increased, loads were applied. The main reasons for this are the increase in fan cooling and the reduction in magnetising current that occur. Improved convective heat transfer occurs due to increased fan speed. The magnitude of this effect depends upon the machine design, though in all cases it is less than linear with fan speed [65].

It is very apparent from table 2.2 that increasing the frequency by 5% results in only a small temperature rise for a fan type load, whilst a 10% increase has a very significant effect. The main reason for the difference in temperature between 52.5 Hz and 55 Hz operation is the effect of frequency upon magnetising current. Figure 2.3 shows typical no load magnetisation curves for a modern induction motor at two frequencies. The true magnetisation curve for the induction machine tested is given in section 3.1.3.

An increase in frequency, above the rated value, will result in a very significant decrease in magnetising current and therefore a reduction in stator current. This will counteract, in part, the increase in current required due to the additional load on the motor. This effect becomes less the more the frequency is increased, since the magnetisation current becomes a smaller proportion of the stator current. This is apparent from table 2.2 since the increase in stator current between the 52.5 Hz and 55 Hz readings is much higher than between the 50 Hz and 52.5 Hz results. The rotor current will increase approximately proportionately with frequency, however the increased heating will not significantly effect reliability due to the large thermal capacity of the cage construction. The induction motor used in the test was a modern machine, purchased in 1985. Older machines tend to be less saturated and will therefore exhibit a greater temperature increase with frequency when fan loaded.

The results of table 2.2 represent a worst case condition since no common motor loads exhibit more than a cube law increase in power demand with frequency. Although ideal fans and pumps follow this cube law relationship, in reality the effect of friction loss reduces the load variation with frequency. Hence, for 52.5 Hz operation the temperature rise will be even closer to that at 50 Hz. Operation above 52.5 Hz should only be allowed in situations where no highly speed dependant loads will be used.

2.4.4. Conclusions

The specification for micro-hydro systems proposed in sections 2.4.2 and 2.4.3 is voltage regulation of +/- 6% of the rated value and frequency regulation within 100 to 105% of the rated value. In comparison to the regulation achieved by most electricity boards in developed countries this frequency specification will appear somewhat lax. However it is more than sufficient for the vast majority of end-uses.

Micro-hydro plant with much larger voltage and frequency variations has been used with no noticeable reduction in the life of the loads. One example of this is a nominally 110V scheme in Columbia, reviewed by Martindale and Apedaile [66]. The site which supplies lighting, heating and small induction motor loads has been operating at between 90 and 130V, and an approximately proportionate frequency range, for five years.

Where just resistive loads are to be used on the system the lower voltage and upper frequency range can be extended. The main disadvantage of extending the lower voltage limit is that variations in lamp output will increase. An extension to 15% below the rated voltage is suggested, in section 2.4.2, as a maximum acceptable limit. The upper frequency limit can be extended to that at which excitation cannot be reliably sustained, due to the operating voltage being below the saturation region for the machine, see fig 2.3. For a standard modern induction motor this has been found experimentally to be up to 50% above rated frequency.

2.5 NEW CONTROL SYSTEM APPROACHES FOR STAND-ALONE INDUCTION GENERATORS

2.5.1 Introduction

As mentioned in section 2.3.7, the standard approach for the voltage and frequency control of stand-alone induction generator systems is a two module approach. The two modules function independently, one module controls the voltage and the other, the load controller, the frequency. With synchronous generators this is achieved using an AVR to control the voltage and a separate regulator to control the speed of the prime mover. As shown in section 2.3.7, and discussed further by Smith [67], the two module approach, when undertaken using induction generators results in systems that are no cheaper or simpler than the present, well proven synchronous generator systems.

A new approach is required if the complexity and cost of induction generator systems is to be reduced to a level that will make them advantageous for micro-hydro systems. The new, and quite radical, approach of this thesis is to allow controlled variations of the supply voltage and/or frequency in order to simplify the control system. The acceptable limits of such supply variations have been set out in section 2.4.

2.5.2 Constant frequency controlled voltage approach

With this approach a standard frequency control module, that produces a variable unity power-factor load, is used and the voltage control module is replaced by a static arrangement of shunt and series capacitors. The reason for using a unity power-factor controller, such as a binary weighted system, is that if the main load on the generator is resistive the voltage will be kept constant, or near constant by the frequency controller. The shunt and series capacitance will then only have to compensate for power factor variations due to the main load and not a compounded effect due to both the main and ballast loads.

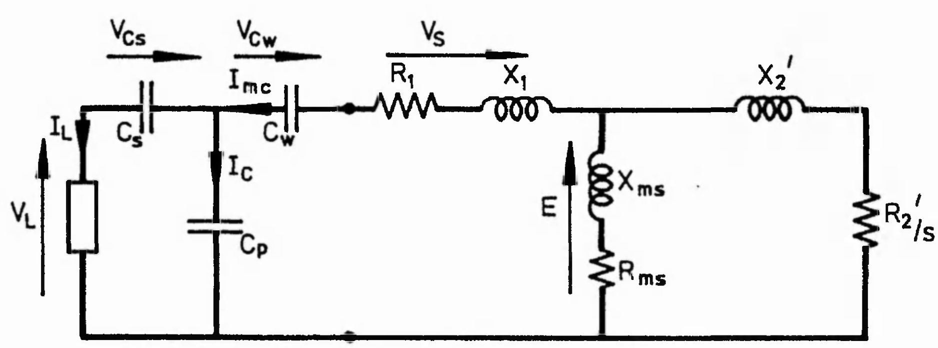


Fig 2.13 Induction generator equivalent circuit incorporating series compensation

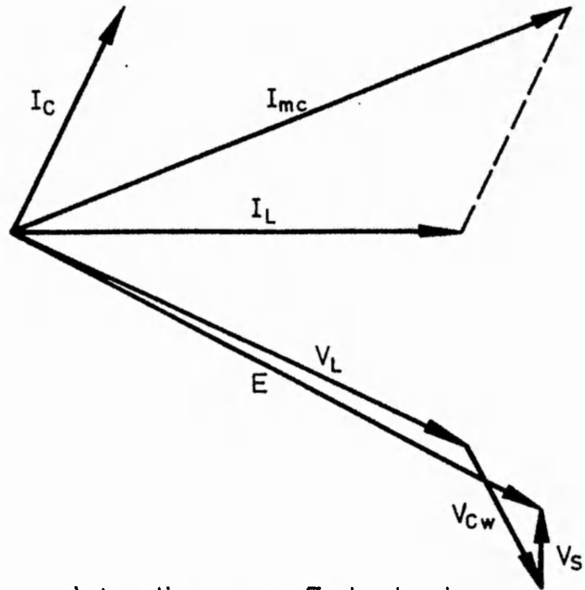
Abdel-Kader and Holmes [68] have shown that by adding series compensation to a standard shunt excited induction generator improved voltage regulation with resistive loading can be obtained. The series capacitor can be placed in series with either the machine stator or the load, as shown in figure 2.14. As the load current rises the VARs produced by the series capacitance increase. The series capacitance compensates, wholly or partially, for the lagging VARs due to the stator and rotor leakage reactances X_1 and X_2.

The loading conditions considered here are different from those examined by Abdel-Kader and Holmes. In this case the resistive load on the generator is kept constant by the electronic load controller and compensation for a variable reactive load is required.

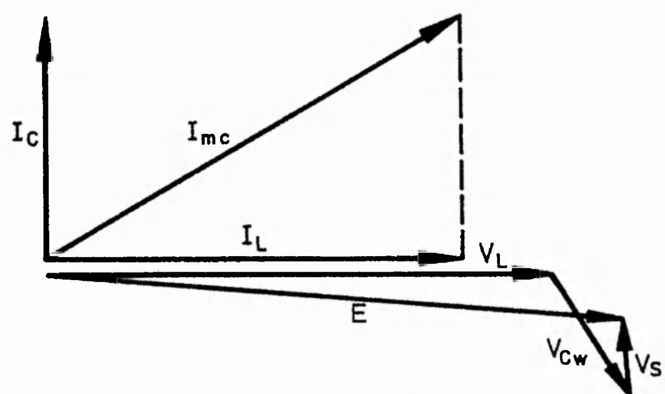
In this case voltage regulation is improved by utilising the series capacitance to increase the magnetising current when the load is resistive and reduce it when the load is inductive. With a reduced magnetising current the excitation capacitance required by the generator will be less, thus allowing the excess leading VARs to compensate for the inductive load.

Phasor diagrams for a range of power-factor conditions are given in figures 2.15 for stator winding series compensation (C_w) and figure 2.16 for load series compensation (C_s). In both cases the voltage across the stator of the machine will decrease relative to the load voltage if the load is made more inductive. A reduction in stator voltage will cause a reduction in the voltage across the magnetising parameters of the machine and hence a reduction in excitation capacitance. This compensates for the reduced leading VARs available due to the presence of the inductive load. By careful choice of shunt and series capacitor values the voltage across the load can be kept approximately constant, over a range of power factors, provided that the resistive load is fixed by the load controller. A computer model, used to investigate this voltage regulation technique, is presented by Smith et al [69].

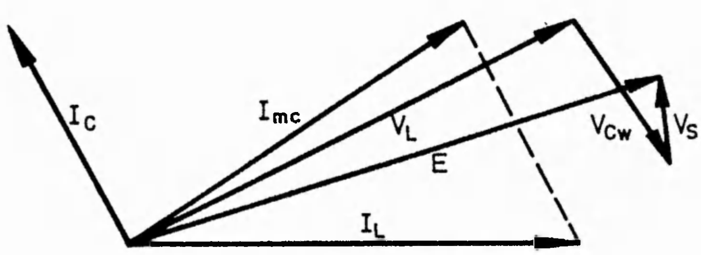
With load series compensation the stator voltage varies from being greater than the load voltage for resistive and leading power factor loads to less than the load voltage for inductive loads. With stator series compensation the stator voltage is always greater than the load voltage, independent of the power factor. Hence the variation in stator voltage with power factor is greater when load series compensation is used. Therefore, in order to provide the same amount of power-factor correction with stator series compensation as with load series compensation the machine must operate with a higher degree of saturation. Standard modern induction machines are already designed to operate with a high degree of saturation, so much so that their no load current can be more than 80% of their full load current. The use of stator series capacitance is not advisable since for most machines the resistive loading would have to be reduced to compensate for the increased saturation. Load series compensation is the preferred method since it provides the same degree of power-factor compensation for less saturation.



a) Leading power factor load



b) Unity power factor load



c) Lagging power factor load

Figure 2.15 Phasor diagrams for stator winding series compensation

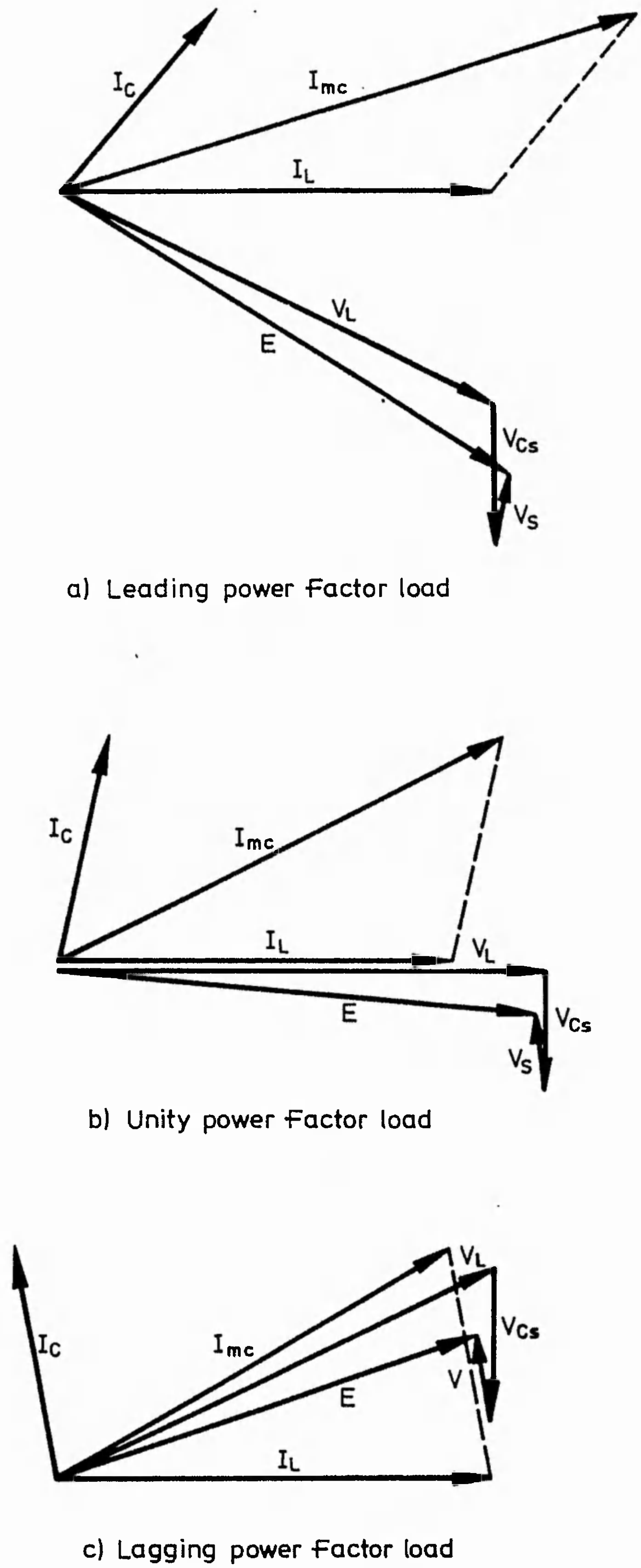


Figure 2.16 Phasor diagrams for load series compensation

The possibility of voltage control by load series compensation is examined in depth in chapters three and four. A computer model is described in chapter three and a comparison between theoretical and experimental results is given in chapter four. Problems due to a sub-synchronous resonance phenomenon, that can occur when induction motor loads are used with this excitation arrangement, are also discussed in chapter four.

2.5.3 Constant voltage controlled frequency approach

As indicated in figure 2.3, for constant voltage operation of an induction machine a small rise in frequency will result in a significant reduction in magnetising current. In addition, provided that standard shunt capacitive excitation is used, extra VARs are produced due to reduced capacitor impedance. The combined effect of reduced magnetising current with frequency and increased leading VARs can be used to advantage for power factor correction of reactive loads. The basic technique, of saturation modification, is essentially the same as with the constant frequency controlled voltage approach described in the previous section. In this case improved regulation with inductive loads is achieved by allowing controlled frequency variations rather than by including series capacitance.

If the resistive loading on the generator is controlled so as to achieve a constant voltage rather than a constant frequency then the operating frequency will depend upon the power factor of the load. Instead of decreasing the ballast load if the generated frequency falls, as in the case of a standard electronic load controller, the ballast load will be decreased if the generated voltage falls. When an inductive load is placed upon the generator the voltage will decrease and the controller will respond by reducing the ballast load. The reduced load upon the turbine will cause an almost instantaneous frequency increase due to reduced slip and an additional gradual increase in frequency due to rising turbine speed. The rising frequency results in an increasing voltage and a corresponding increasing load. A stable operating point will be reached when the load produced by the generator increases sufficiently to match the output power of the turbine.

The increase in frequency will depend principally upon the power factor of the load and the degree of saturation of the induction machine. Since modern induction machines tend to be highly saturated the frequency regulation with variations in load power factor is quite small. In chapter three a computer model is described that determines the theoretical variation in frequency with load power factor for an induction generator, of known parameters, operated at a constant voltage. In chapter four, experimental results are compared with those of the computer model.

In order to minimise the frequency variations of the generator the power factor of the ballast load should be as close to unity as possible. For this reason a phase-angle type controller is not the preferred solution. Two approaches were considered; the first used a binary-weighted arrangement and the second a variable mark-space ratio chopper. The binary weighted controller used the approach described in section 2.2.2, except that in this case voltage was sensed and controlled. The single phase binary weighted controller is described in chapter five.

The mark-space ratio approach has not been used for load control before. The ballast load is connected to the rectified output of the generator. By chopping into the resistive load with a variable mark-space ratio, at a frequency well above the generated frequency, a variable unity power-factor load is achieved. Single and three phase variable mark-space ratio chopper controllers were designed, built and tested. Full details of these controllers are presented in chapter five. Both types of controllers have become known as induction generator controllers (IGCs) to differentiate them from standard load controllers.

2.5.4 Controlled voltage controlled frequency

The constant frequency controlled voltage and constant voltage controlled frequency approaches outlined above have the significant advantage, compared to the conventional approach, of requiring just a single power module, that is no more complicated than a standard load controller. However, the simplest and cheapest control system for micro-hydro would be an approach that required no control modules. The main problem with such an approach is the wide speed variation of the prime-mover with load and its resultant effects upon voltage regulation. As mentioned in section 1.2.1, Kittredge [16] has determined that low specific speed pumps have only a small percentage overspeed. By using such a pump in conjunction with a generator using series compensation reasonable voltage regulation may be possible. It is likely that the wide voltage and frequency regulation that will result from this very basic control system will render the system unsuitable for most inductive loads. However the regulation may be sufficient for basic lighting and heating loads.

Tests were performed on a low-specific speed pump in order to determine its characteristics when used as a turbine. The experimental arrangement and turbine-mode characteristics are given in chapter four. The characteristics were then entered into the computer simulation, of chapter three, in order to determine the voltage and frequency regulation that can be achieved when coupled to an induction generator with combined shunt and series excitation.

2.6 CONCLUSIONS

At present, synchronous generators are used on stand-alone micro-hydro systems despite the advantages of the induction generator in terms of cost and reliability. The main reasons for this are the expense and complexity of voltage control for induction generators.

The approach taken in this thesis is one of reducing the complexity of induction generator voltage and frequency control. As a result, the reliability and economic viability of this generating method will be improved. These advantages are gained at the expense of poorer voltage and/or frequency regulation. Careful consideration has been given to the acceptable limits of such supply variations. The results of computer modelling and experimental testing, recorded in the following chapters, indicate the degree of acceptability of the three control approaches considered.

CHAPTER 3

COMPUTER MODEL OF TURBINE-GENERATOR SYSTEM

3.1 INTRODUCTION

In this chapter a computer model of a standard three-phase self-excited cage induction machine driven by a hydraulic turbine is presented. Other forms of prime-mover, such as diesel engines or wind-turbines, can be modelled using the computer program, though these applications are beyond the scope of this project.

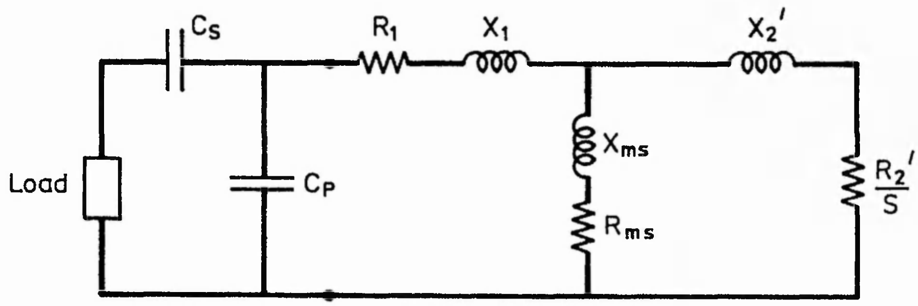


Figure 3.1 Series and shunt excitation for a self-excited induction generator

The electrical modelling is based upon the 'exact' equivalent circuit representation of the induction machine and allows for both shunt and series excitation to be used, as shown in figure 3.1. Variations of the magnetising reactance with both voltage and frequency, the core-loss with voltage and the leakage reactances with frequency have been included in the model. The prime-mover power verses angular velocity characteristic has been entered in the form $k0 + k1\omega + k2\omega^2 + k3\omega^3 + ... k12\omega^{12}$, so that almost any turbine can be closely modelled.

The model was checked by comparing experimental results with results from the model. First, it was necessary to accurately determine the parameters of the test machines for use in the model.

3.2 TEST MACHINES

3.2.1 Induction machines

Manufacturer	GEC	Mawdsley
Rotor type	Cage	Wound
Number of phases	3	3
Rated output	2.2 kW	6 kW
Rated phase voltage	220/240 Volts	240 Volts
Rated phase current	5.1/4.7 Amps	11.5 Amps
Rated speed	1420 RPM	1440 RPM
Year of manufacture	1985	1951

Table 3.1 Manufacturer's data for the two test machines

Table 3.1 gives the manufacturer's data for the two induction machines that were used as test machines. The G.E.C. machine was used predominantly, because of its cage rotor and recent manufacture. The wound rotor machine was used primarily for comparison purposes.

3.2.2 Centrifugal Pump

A centrifugal pump was operated in reverse in order to establish its performance as a hydraulic turbine. The pump, normally driven by a 7.5 kW three-phase, four-pole cage induction motor, was a Worthington Simpson double-entry pump. The impeller of the pump was non-standard, since its diameter had been reduced by 10% as part of a previous research project. This fact does not invalidate its use as a typical centrifugal pump, since it is common practice for a range of impeller diameters to be used with the same size of casing.

3.3 DETERMINATION OF THE PARAMETERS OF A THREE-PHASE CAGE INDUCTION MACHINE

3.3.1 Introduction

Irrespective of whether an induction machine is modelled as a motor or generator the same standard induction machine parameters must be determined. When operation over a range of voltages and frequencies is anticipated, as in the case of a stand-alone system, parameter variations with both voltage and frequency must be included in the model. The most important parameter to be determined is the magnetising reactance, since, for fixed excitation arrangements of the type outlined in section 2.5, it is this parameter that has the greatest effect upon the generated voltage. The leakage reactances, being of smaller magnitude and less effected by saturation, are of secondary importance.

Of the resistive parameters, the stator resistance is the most critical, since its value is used when obtaining the other machine parameters and also for predicting the regulation and losses of the generator. In order to obtain accurate values for the variation of stator resistance with temperature, thermocouples were fitted in both sets of end windings of the G.E.C. machine.

Rotor resistance also varies with temperature. This effect can be quite marked, particularly for totally enclosed machines with their more limited cooling. The 2.2 kW G.E.C. motor was a totally enclosed machine and hence an appreciable temperature variation of rotor resistance with load was expected. Fortunately, such variations do not effect the calculation of the other parameters in the equivalent circuit and also, as illustrated in section 3.5.4, variations of rotor resistance have little effect upon the voltage and frequency regulation of the machine when used as a stand-alone generator. Since the variation of rotor resistance with temperature is not of great importance, unless very accurate efficiency calculations are to be made, and the task of determining the temperature of the rotor bars is difficult, no attempt was made to include this in the model.

The standard tests for the determination of three-phase induction machine equivalent circuit parameters are usually a d.c stator resistance test, a locked rotor test and a no-load test. In addition an a.c. stator resistance test, with rotor removed, and a full load test were carried out. Initially, the simplifying assumption of equal rotor and stator leakage reactances was made. This is a fairly standard assumption when machine parameters are obtained by electrical tests. It is used because, as a result of the leakage reactances being much smaller than the magnetising reactance, accurate separation of the two parameters is difficult. Also, as Grantham has shown [70], little difference in motor-mode performance will result if they are unequal. The computer model is used to show that this holds for a stand-alone induction generator. This is illustrated in section 3.5.5.

3.3.2 Stator resistance tests

Stator resistance varies significantly with temperature and therefore machine loading, though, with modern induction machines operated under normal conditions, there is always an appreciable stator current due to the high degree of saturation. In order to accurately determine the stator resistance, both for use in the computer model and in the tests used to obtain the other machine parameters, the temperature of the stator windings must be known.

The G.E.C. machine was supplied with type K thermocouples inbedded in its end windings on both sides of the stator core. Calibration tests were required for the following reasons:-

1) To compensate for thermocouple inaccuracies, relative to the British standard characteristic.

2) To compensate for offsets and other errors in the voltage to temperature conversion electronics.

3) To take account of the difference between the mean temperature of the stator winding and the temperature at the position of the thermocouple.

The method of calibration and the results for the G.E.C. machine are presented in appendix B1.

For accurate modelling, the value of stator resistance required is the a.c. value at the temperature considered. The a.c. value is required in order to account for the eddy current losses that arise. It can be obtained by a rotor removed test, as recommended by Ware [71]. For small machines, such as those under test, the a.c. and d.c resistance values are likely to be almost equal, since many conductor turns are used per slot and hence current displacement effects will be limited. However, the test was performed for completeness and the results are presented in appendix B2. The results were taken at a current near the rated value, and the flux level was determined by calculating the voltage across the leakage reactance. A no-load test at synchronous speed and the voltage required to give the same flux level was carried out in order to estimate the core-loss component for the rotor removed test. The results are given in appendix B4.

3.3.3 Locked rotor test

The locked rotor test was performed with the machine already warmed up from a full-load test. The voltage supplied to the machine was increased until the full load current was reached. Results were taken quickly so as to minimise the temperature changes in the machine. The results are presented in appendix B3.

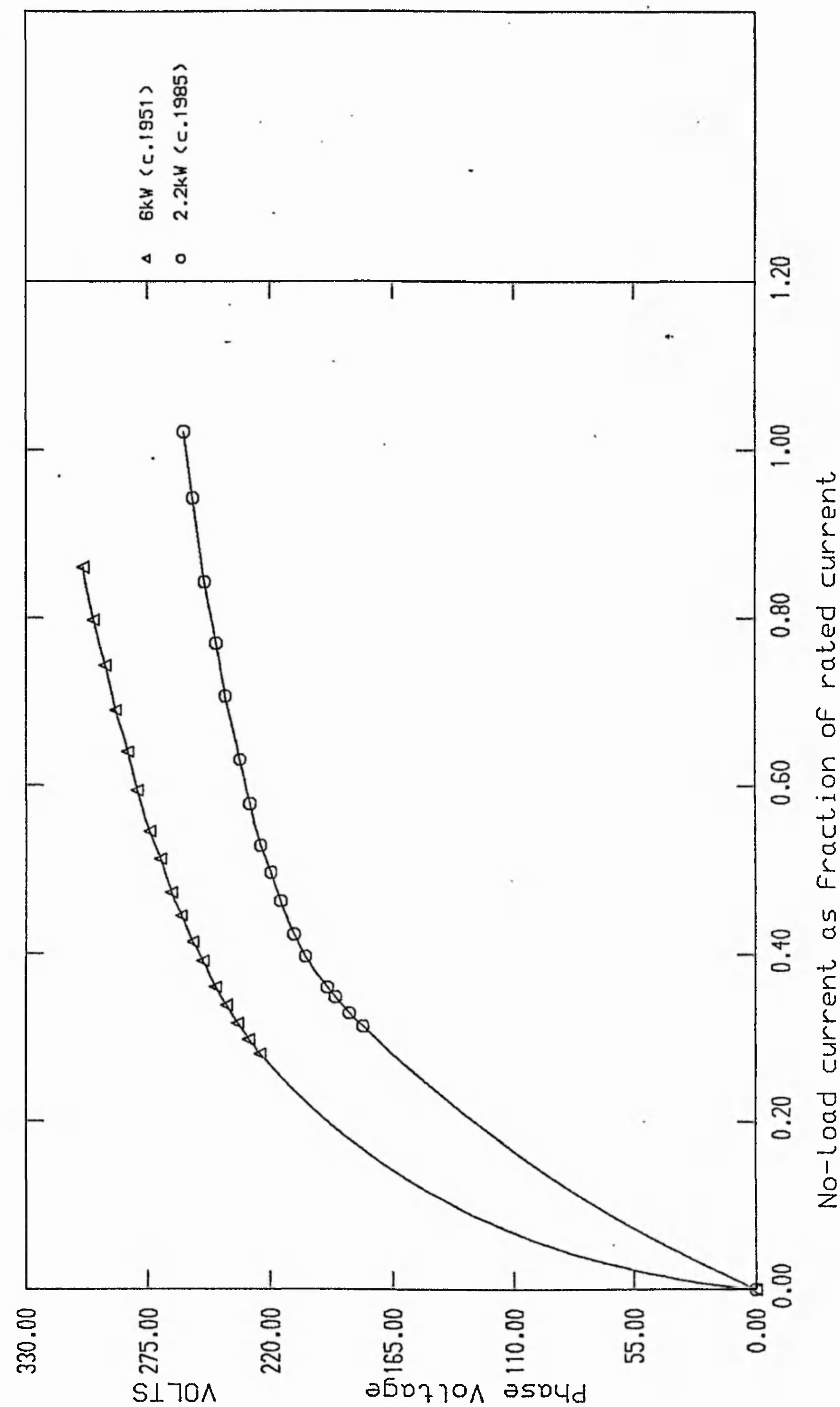


Figure 3.2 Voltage against current at no load for the two test machines

3.3.4 No load tests with the motor driven at synchronous speed

By driving the induction motor at synchronous speed the current in the rotor of the machine will be zero. Hence, using a three-phase wattmeter, it is possible to determine the values of X_{ms} plus X_1 and R_{ms} plus R_1. In order to determine the variation of the magnetising resistance and reactance with the magnetising voltage, E, tests were performed from a voltage close to the onset of saturation through to the voltage at rated current. In addition, readings were taken at a voltage close to that used in the locked rotor test and at a voltage corresponding to a similar flux level to that used in the rotor removed test. Results are given in appendix B4. For comparison purposes, the no-load tests were repeated, for the saturation region of the much older wound rotor machine. These results are also presented in appendix B4. A graph of voltage against current for each machine is presented in figure 3.2. These graphs show that the more modern machine operates at a much greater level of saturation.

3.3.5 Full load test

This test was used to obtain a value of rotor resistance under normal frequency conditions. It is well known that a full load test yields a more accurate value of rotor resistance than the locked rotor test, since the frequency of the rotor current and hence the skin effect are within their normal operating range. Results are given in appendix B5. Using the values determined for the other five machine parameters, at the same temperature and saturation level, along with the experimental result for slip, the value of R_2 can be determined.

3.3.6 Calculation of stator resistance

The a.c. stator resistance was determined from the results of the rotor removed test (appendix B2) and the no-load test, performed at the same flux level (appendix B4). The calculations are presented in appendix B6. The a.c. value for the stator resistance was found to be 1.5% higher than its d.c. value. Whilst this difference could be explained in terms of experimental error, the result is consistent with what one would expect for a machine of this type [72].

3.3.7 Calculation of locked rotor resistance and stator leakage reactances

For a locked rotor test at reduced voltage the volt-amps supplied to the magnetising parameters, shown as a series arrangement in figure 3.3, are small but not insignificant.

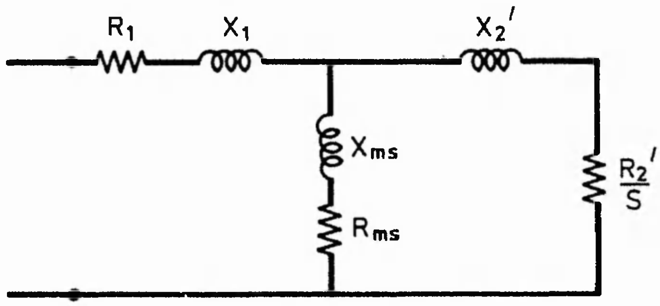


Figure 3.3 Induction machine equivalent circuit with series arrangement of magnetising parameters

The total impedance of the circuit, Z_{lr} is:-

$$Z_{lr} = \left(R_1 + jX_1\right) + \frac{\left(R_{ms} + jX_{ms}\right)\left(R_2 + jX_2\right)}{\left(R_{ms} + R_2\right) + j\left(X_{ms} + X_2\right)} \qquad \text{..............3.3.1}$$

Multiplying the numerator and denominator of the second term of equation 3.3.1 by the conjugate of the denominator and separating into real and imaginary parts yields:-

$$R_{lr} = R_1 + \frac{R_{ms}^2 R_2 + R_2^2 R_{ms} + X_{ms}^2 R_2 + X_2^2 R_{ms}}{\left(R_{ms} + R_2\right)^2 + \left(X_{ms} + X_2\right)^2} \qquad \text{..............3.3.2}$$

$$X_{lr} = X_1 + \frac{R_2^2 X_{ms} + R_{ms}^2 X_2 + X_{ms}^2 X_2 + X_2^2 X_{ms}}{\left(R_{ms} + R_2\right)^2 + \left(X_{ms} + X_2\right)^2} \qquad \text{..............3.3.3}$$

Where $Z_{lr} = R_{lr} + jX_{lr}$ 3.3.4

Wattmeter readings yield values for R_{lr} and X_{lr}. However, despite knowing the value of the stator resistance and assuming that the stator and rotor leakage reactances are equal, there is insufficient information to determine the rotor resistance and leakage reactances. Further information is required, which can be obtained from the no load test of section

3.3.4. This test, when carried out at the same magnetising flux level as the locked rotor test, yields a further two equations, containing three of the unknowns. The total impedance of the circuit, for the no load test, is as follows:-

$$Z_{nl} = R_1 + R_{ms} + j(X_1 + X_{ms}) \qquad \text{.............. 3.3.5}$$

Hence $$R_{nl} = R_1 + R_{ms} \qquad \text{.............. 3.3.6}$$

$$X_{nl} = X_1 + X_{ms} \qquad \text{.............. 3.3.7}$$

Where $$Z_{nl} = R_{nl} + jX_{nl} \qquad \text{.............. 3.3.8}$$

Again, wattmeter readings yield the no load resistance and reactance. Hence, since R_1 is known the value of R_{ms} can be calculated. Also, substituting for X_{ms}, from equation 3.3.7 into equations 3.3.2 and 3.3.3, yields:-

$$R_{lr} = R_1 + \frac{R_{ms}^2 R_2 + R_2^2 R_{ms} + [X_{nl} - X_1]^2 R_2 + X_2^2 R_{ms}}{(R_{ms} + R_2)^2 + ([X_{nl} - X_1] + X_2)^2} \qquad \text{.............. 3.3.9}$$

$$X_{lr} = X_1 + \frac{R_2^2 [X_{nl} - X_1] + R_{ms}^2 X_2 + [X_{nl} - X_1]^2 X_2 + X_2^2 [X_{nl} - X_1]}{(R_{ms} + R_2)^2 + ([X_{nl} - X_1] + X_2)^2}$$

.... 3.3.10

These two equations in two unknowns can be solved using the Newton Raphson method [73], as explained in appendix C. A computer program was produced, that uses this approach to determine the rotor resistance and leakage reactances. This is listed in appendix D.

3.3.8 Calculation of the core loss and magnetising reactance for a range of voltages

The core-loss and magnetising reactance, for a range of voltages, can be determined using the results of the no-load tests conducted at synchronous speed. Wattmeter readings yield results for the circuit resistance and reactance, as given in equations 3.3.6 and 3.3.7. Using the results for the stator resistance and leakage reactance, calculated in sections 3.3.6 and 3.3.7 respectively, the values for X_{ms} and R_{ms} can be obtained directly. For ease of modelling the generator a parallel representation of core-loss and magnetising reactance, R_p and X_p, was used. The results for the G.E.C. machine are presented in appendix B7.

The results for X_p show an appreciable variation with both magnetising voltage and terminal voltage. This must be taken into account if the machine is to be accurately modelled as a stand-alone generator. Interpolation, based upon a look-up table, is one possible approach though it would tend to both lengthen and complicate the model and, for best results, would involve producing the table from a smooth curve fitted to the data. A better approach is to relate each of the two parameters to the magnetising voltage, E, by mathematical functions that show a good correlation to the experimental results. These functions can then be used in the model.

A computing package was used to determine appropriate mathematical functions for both X_p and R_p. The package used was the Numerical Algorithm Group's (NAG) Generalised Linear Interactive Modelling package (GLIM) [74]. The technique used in this package is outlined in appendix E.

$$X_p = X_c + X_m . E \qquad \text{..............3.3.11}$$

For the case of X_p, a good fit to the results, which are shown in figure 3.4, can be obtained with the simple first order model of equation 3.3.11. However, it can be seen from the residuals, plotted in figure 3.5, that this model is not fully valid. The main reason for this is that for lower values of magnetising voltage, E, saturation is reduced and the value of X_p is therefore tending towards a constant value. Rather than adding extra terms to the model to accommodate this, the simple model was kept and the results for values of E below 200V ignored. The new results, shown in figures 3.6 and 3.7, indicate that this model is valid for this smaller voltage range. Since this voltage range is the normal operating range for the machine the model is suitable for most purposes.

For the case of R_p no significant variation with the magnetising voltage was found and so an average value was taken.

3.3.9 Calculation of rotor resistance

The calculations of sections 3.3.6, 3.3.7 and 3.3.8 yield results for all the equivalent circuit

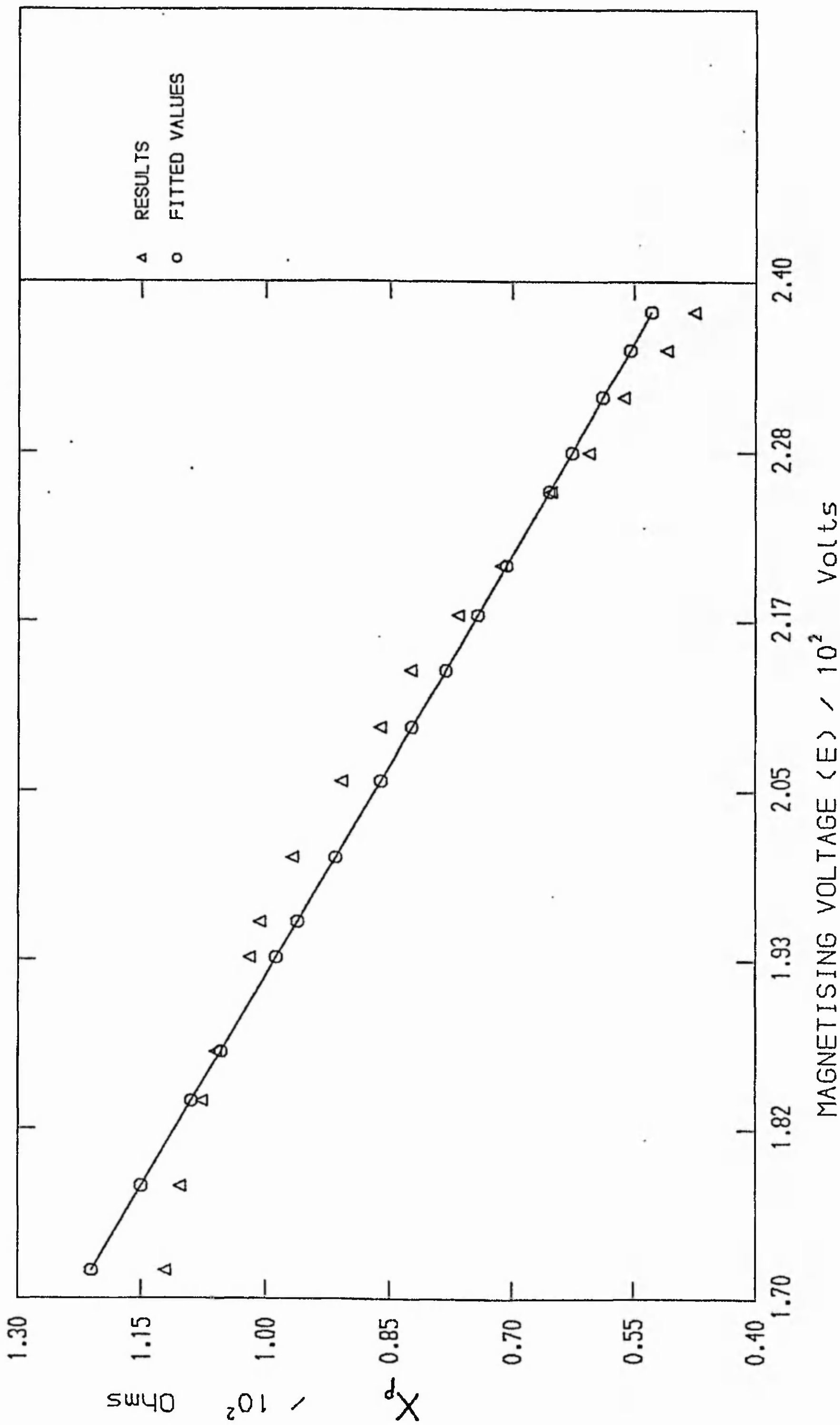


Figure 3.4 GLIM filt of magnetising reactance against magnetising voltage for the range 170 -240 Volts

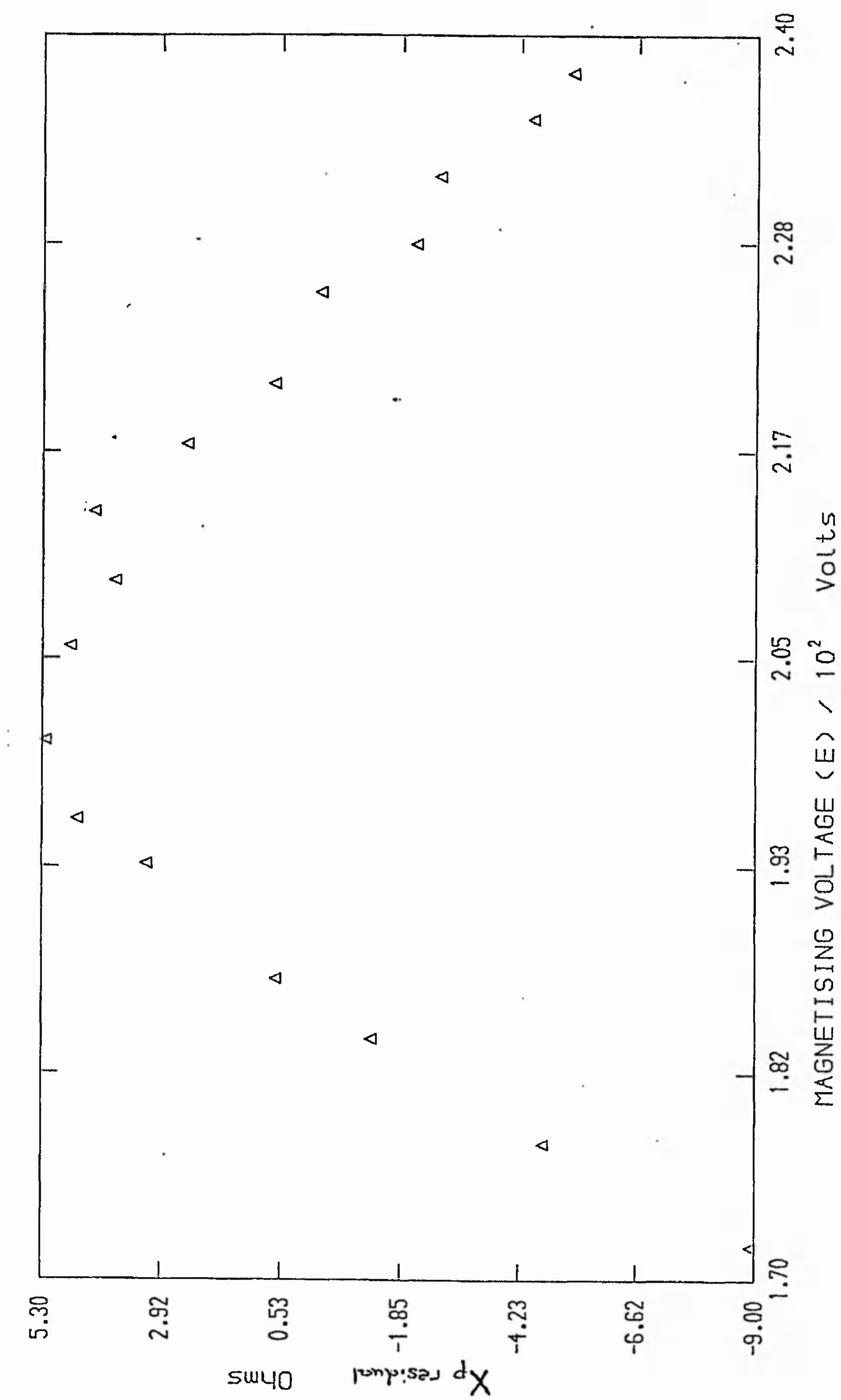


Figure 3.5 Plot of residuals from GLIM fit of magnetising reactance against magnetising voltage for the range 170 - 240 Volts

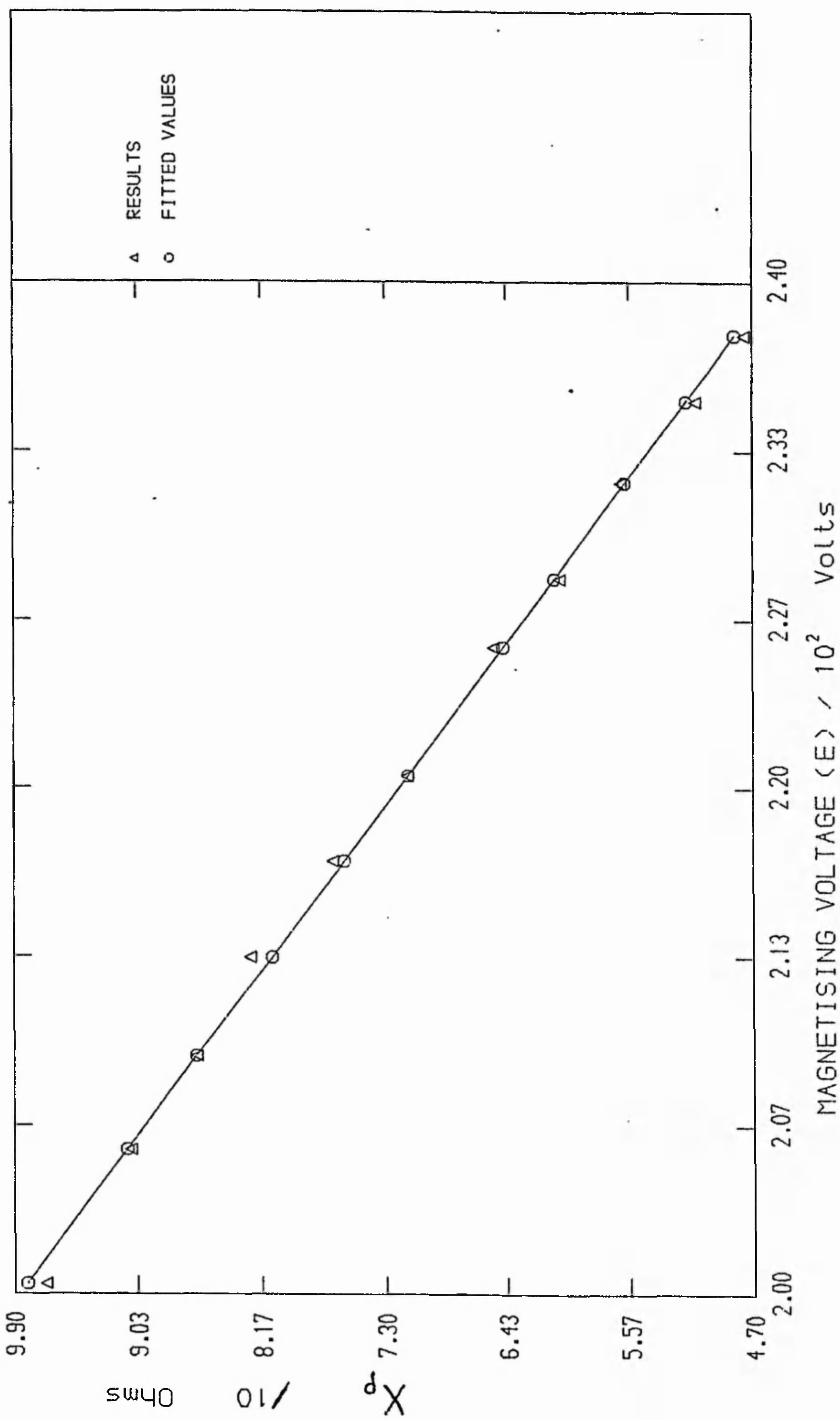


Figure 3.6 GLIM fit of magnetising reactance against magnetising voltage for the range 200 - 240 Volts

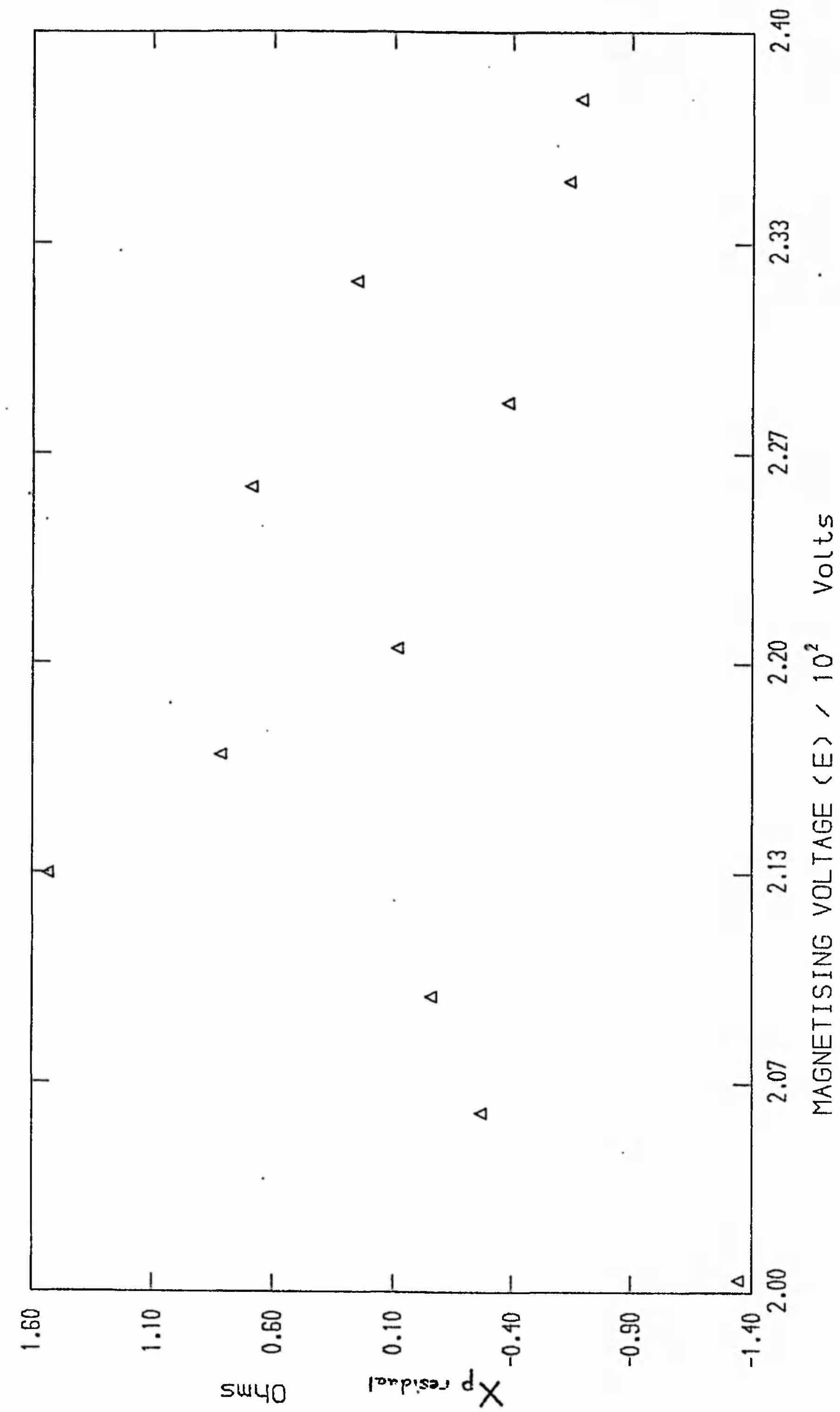


Figure 3.7 Plot of residuals from GLIM fit of magnetisng reactance against magnetising voltage for the range 200 - 240 Volts

parameters of the machine. However, the rotor resistance value will be higher than that expected for normal operating conditions, because of the greatly increased skin effect under locked rotor conditions. The full load test results, of section 3.3.5, can be used to obtain a value of R2 under normal frequency conditions, using the calculated values for all the other parameters.

The method of calculating R2 is presented in appendix B8, and the computer program written for this purpose is presented in appendix F. The value of R2 calculated from the full load test, see table 3.2, was 23% less than the locked rotor result.

PARAMETER	VALUE
R_1	2.923 Ω*
R_2 (Locked Rotor)	2.13 Ω
R_2 (Full Load)	1.73 Ω
X_1 & X_2 (50Hz)	3.98 Ω
X_c (50Hz)	365 Ω
X_m(50Hz)	-1.332 ΩV^{-1}
R_c	2054 Ω
R_m	0 ΩV^{-1}

* Value at 22 degrees centrigrade

Table 3.2 Experimental results for the parameters of the 2.2 kW cage machine

3.3.10 Conclusions

The results obtained for the parameters of the 2.2 kW G.E.C machine are listed in table 3.2. As explained in the previous section, the full-load test value for R2 is more acceptable than the locked rotor result since it was obtained under normal rotor frequency conditions. The rotor leakage inductance will also vary with rotor frequency, though to a smaller extent than rotor resistance. Under locked rotor conditions the current is displaced towards the outside of the bar due to the higher leakage reactance at the bottom of the bar. This in turn displaces the leakage flux paths causing a general increase in path length and reduction in permeance. This effect is less pronounced than that for rotor resistance and is offset, for calculation purposes, by an increase in stator leakage reactance, due to reduced mutual flux at the low supply voltage used for the locked rotor test.

3.4 COMPUTER MODEL FOR TURBINE GENERATOR SYSTEMS

3.4.1 Introduction

The main purpose of the computer model was to enable a comparison to be made between the voltage and frequency regulation produced by the CFCV and CVCF control approaches, in order to help determine which was the preferred approach. In addition the model was used to help assess whether the CVF approach is a realistic option for turbines which do not exhibit large overspeeds. For these two purposes a high degree of accuracy is not required and so, for simplicity, a lumped parameter model was developed.

3.4.2 The variation of induction machine parameters with frequency

In section 3.3.8 the variations of the magnetising reactance and core-loss with voltage were determined. Both of these values and the stator and rotor leakage reactances vary with frequency. The extent and significance of these variations must be assessed and if necessary included in the computer model.

For the case of core-loss, the hysteresis and eddy current losses must be considered separately. The standard equation for hysteresis loss, P_h, is:

$$P_h = k_h \, Vol \, f \, B_{max}^{\,n} \qquad \text{.............3.4.1}$$

Where k_h is the hysteresis constant relating loop area to B_{max}

Vol is the volume of core material

f is the supply frequency

B_{max} is the maximum flux density

n is an experimentally determined constant which depends upon the material of the core and is usually between 1.5 and 2.5

Since the magnetising voltage, E, is proportional to the product of flux density and supply frequency, then for a constant magnetising voltage:

$$B_{max} \propto \frac{1}{f} \qquad \text{.............3.4.2}$$

Hence $P_h \propto f^{1-n}$ 3.4.3

Therefore, for a constant magnetising voltage, the hysteresis loss decreases with frequency.

For the eddy current loss the relationship with frequency is normally given as:

$$P_e = k_e \left(B_{max}\, t\, f\right)^2 \qquad \text{..............3.4.4}$$

Where k_e is a constant determined by the core material
t is the lamination thickness

By applying equation 3.4.2 it is clear that for a constant magnetising voltage the eddy current loss will not vary with frequency.

If the core-loss variation with frequency is to be included in the model then separation of eddy current and hysteresis losses must be performed and the value of the constant, n, determined. Because of the extra experimental work required, the addition of extra complexity to the model and the relatively small significance of core-loss variation with frequency, as shown in section 3.5.6, such a relationship was not included in the model.

For the magnetising reactance the variation with frequency is very significant because the machine operates with a high degree of core saturation. Figure 3.8(a) shows the variation in magnetising current with magnetising voltage for a typical machine at two different supply frequencies. This characteristic can be used to determine the vartiation in the magnetising inductance with magnetising voltage and this is shown in figure 3.8(b). The near straight line relationship in the saturation region is known from the GLIM fit of section 3.3.8. For variations in frequency the value of L_c will remain constant and the gradient, L_m, will increase linearly with frequency. Hence the relationship between L_p and magnetising voltage and frequency is:

$$L_p = L_c + L_m E \frac{\omega_{50}}{\omega} \qquad \text{..............3.4.5}$$

Where L_m is the value at 50 Hz
ω_{50} is the angular velocity at 50 Hz

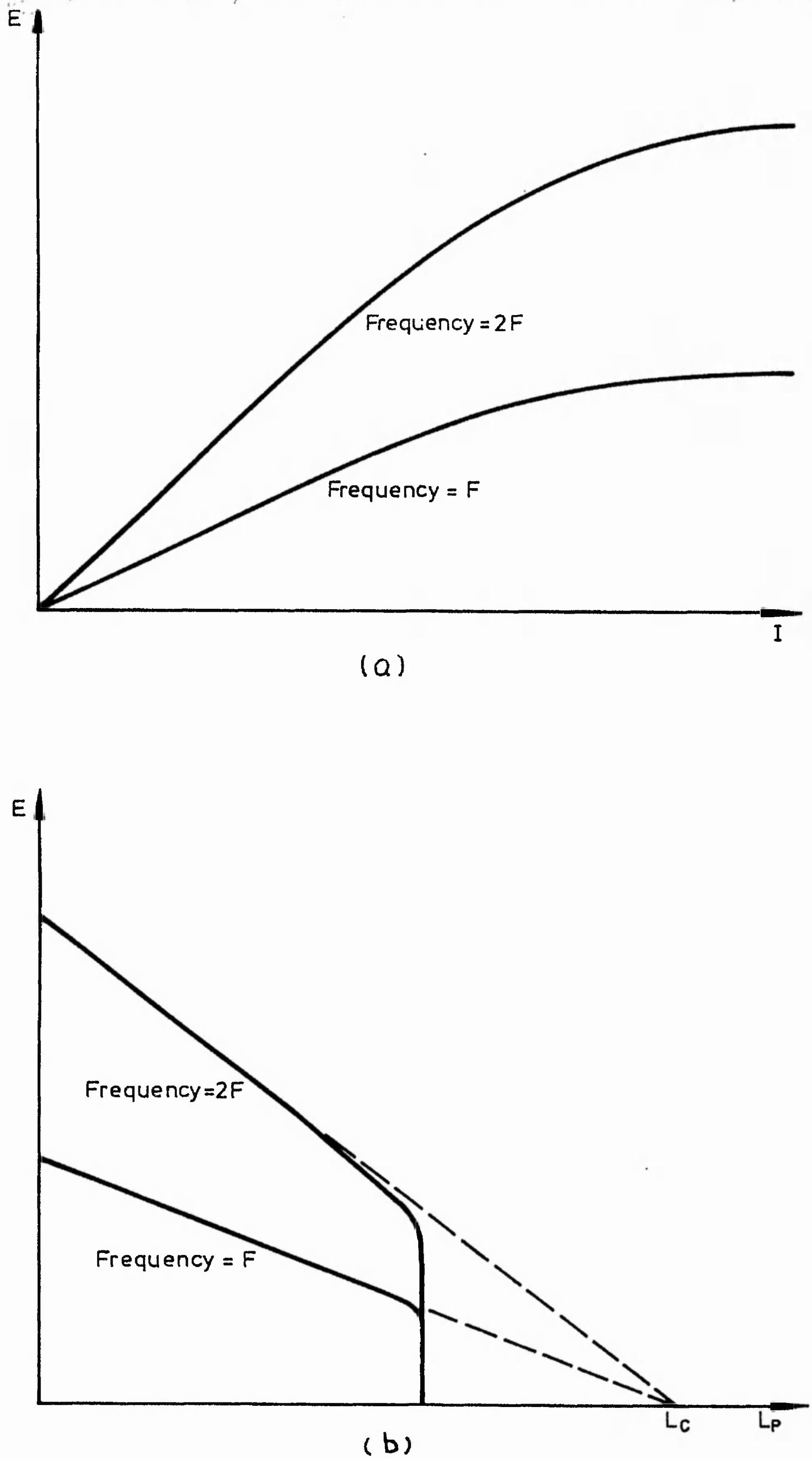


Figure 3.8 Variation of (a) magnetising current and (b) magnetising inductance with magnetising voltage

The stator and rotor leakage reactance values will vary directly with frequency and also as a result of saturation effects. The saturation effects are small, in this case, since the air gap reluctance dominates over the reactance of the iron. Hence, L_1 and L_2 were taken as constants for the computer model.

3.4.3 Theoretical basis of the computer model

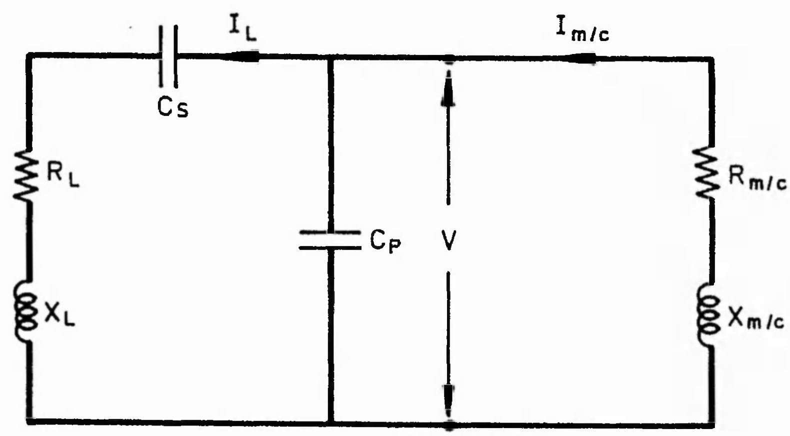


Figure 3.9 Basic representation of a stand-alone induction generator system

Figure 3.9 shows a basic, per phase, representation of an induction generator, with series and shunt excitation, supplying a lagging power factor load. This is the basis for the electrical system to be modelled. For this initial representation, the machine parameters are lumped together into a resistive component, $R_{m/c}$, and a reactive component, $X_{m/c}$. The current drawn from the induction machine, $I_{m/c}$, can be expressed as:

$$I_{m/c} = \frac{-V}{R_{m/c} + jX_{m/c}} \qquad \text{..............3.4.6}$$

Equating the real and imaginary parts of $I_{m/c}$ to the load current, I_L, and the shunt capacitor current, I_p, yields:

$$-\frac{V R_{m/c}}{R_{m/c}^{2} + X_{m/c}^{2}} = I_L \frac{R_L}{Z} \qquad \text{..............3.4.7}$$

and $$\frac{V X_{m/c}}{R_{m/c}^{2} + X_{m/c}^{2}} = I_L\left(X_{Cs} - X_L\right) + \frac{V}{X_{Cp}} \qquad \text{..............3.4.8}$$

Where Z is the total impedance of the load and series capacitance
RL and XL are the respective resistive and reactive portions of the load

Substituting for V, in both equations, yields:

$$-\frac{R_{m/c}}{R_{m/c}^{2} + X_{m/c}^{2}} = \frac{R_L}{Z^2} \qquad \text{..............3.4.9}$$

$$\frac{X_{m/c}}{R_{m/c}^{2} + X_{m/c}^{2}} = \frac{\left(X_{Cs} - X_L\right)}{Z^2} + \frac{1}{X_{Cp}} \qquad \text{..............3.4.10}$$

For a machine of known parameters, driven at a known speed, excited with known values of series and shunt capacitance and loaded with a known impedance, the magnetising voltage and slip are the only two unknowns of equations 3.4.9 and 3.4.10. Provided that sensible estimates for these parameters are available they can be determined by the Newton Raphson method, as outlined in appendix C.

Sensible estimates for the slip, s, and magnetising voltage, E, can be obtained by first solving the two equations for a well understood combination of excitation and load conditions. Then by gradually changing the load and capacitor values, using as estimates for s and E the results of the previous iterative procedure, a solution for the conditions of interest can be obtained. A well understood combination of excitation and load conditions are no load, rated frequency, and sufficient shunt capacitance to power factor correct the machine when operated as a motor. Under such conditions the slip will be a very small negative value and the magnetising voltage will be close to the rated voltage of the machine.

To fully model the turbine-generator system the power input from the turbine must be matched to the power output of the generator and the losses in the system. In order to closely model a wide range of turbines the turbine power-speed characteristic has been entered as a twelfth order equation:

$$P_{turb} = P_{max} \{k_0 + k_1\omega + k_2\omega^2 + k_3\omega^3 + ...k_{12}\omega^{12}\} \qquad ....3.4.11$$

Where ω is the angular velocity of the turbine.

This may seem excessive, especially as the characteristics of most impulse turbine can be closely modelled by a second order equation. However, such a high order equatiion is required if the steep overspeed characteristics of low specific speed reaction turbines are to be modelled. A practical example of such a characteristic is given in chapter 4. Five losses are included in the model; power dissipated in the stator winding, power dissipated in the rotor bars, core-loss, and both turbine and generator friction and windage losses.

By including the turbine power-speed characteristic in the model an extra unknown, that of the turbine-generator shaft speed, is added. However, by including an additional equation, that balances the turbine input power with the electrical output power and system losses, the shaft speed can be determined using the Newton Raphson method for three equations involving three unknowns.

3.4.4 Outline of the computer model

A full listing of the computer program is presented in appendix G. The principal steps are as follows:-

1) The parameters are entered as a list from one of a choice of data files. Included in the list are the machine parameters, values of shunt and series capacitance, turbine parameters, load power factor and initial estimates of the magnetising voltage, slip and shaft speed. These can then be changed as required, either at the start or during the program.

2) The maximum load current is determined using the values of generator rating and estimated magnetising voltage in the parameter list.

3) For 21 values of load current, in even steps from zero to the maximum value, the iterative procedure is carried out. For the zero load current case the estimated values, for E, s and ω are taken from the parameter list. For subsequent iterations the results from the preceding iterative procedure are used to provide the three initial estimates. For each value of load current, the iterative procedure is judged to be complete when, for each of the three parameters, the adjustment required is less than 0.0001% of the initial value. If the value of the magnetising voltage, E, falls below its minimum value, as given by the GLIM fit for

the magnetising reactance, the result is not recorded.

4) The results from the model are presented in tabular and graphical form. In addition, experimental results can be entered and plotted against the characteristics generated by the model.

3.4.5 Conclusions

A comprehensive model of the turbine-generator system has been presented. It includes parameter variations with both voltage and frequency. This model can be used to analyse the steady-state performance of water-powered stand-alone three-phase induction generators with shunt or series-shunt capacitive excitation.

3.5 ASSESSMENT OF THE ACCURACY OF THE COMPUTER MODEL

3.5.1 Introduction

In order to assess the accuracy of the computer model a series of tests were performed for both shunt and combined series-shunt capacitive excitation of the 2.2 kW machine. In each case the speed of the prime-mover, in this case a d.c. motor, was maintained constant and results for a range of loads, from no load to full load, were taken. The experimental results were then compared with those predicted by the computer model.

3.5.2 Comparison of theoretical and experimental results for shunt excitation alone

Four sets of experimental results were taken, for a range of prime-mover shaft speed from 1500 rpm to 1725 rpm, for a per phase shunt capacitance of 45 μF. These results, along with results from the computer model, are shown in figure 3.10. It can be seen that there is good agreement between the model and experiment.

3.5.3 Comparison of theoretical and experimental results for combined series and shunt excitation

Five sets of experimental results were taken at 1500 rpm and a range of shunt and series capacitance values. These results, along with results from the computer model, are shown in figure 3.11. Again, there is a good agreement between the model and experiment.

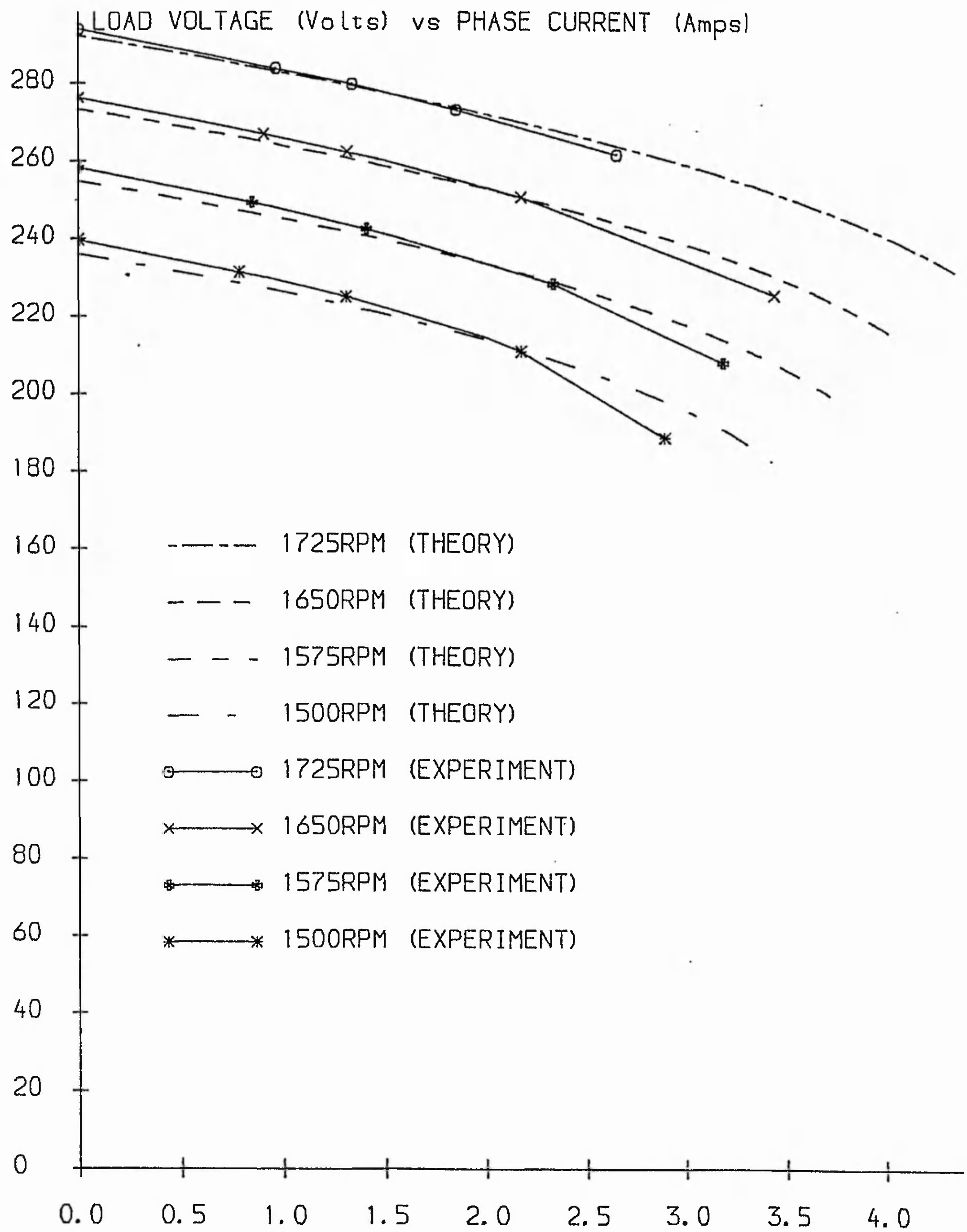


Figure 3.10 Theoretical and experimental results of load voltage against load current, for a shunt excited generator at four shaft speeds

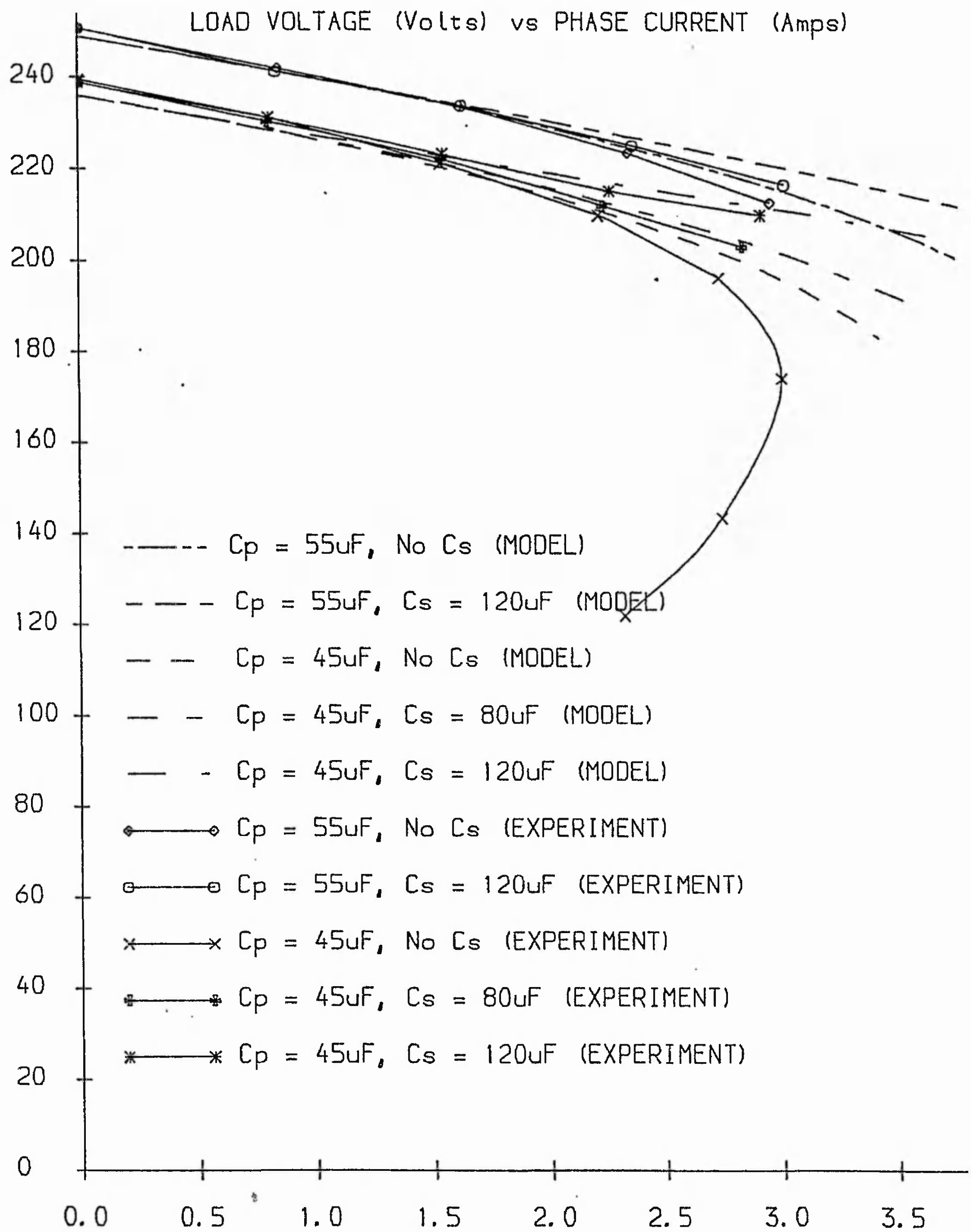


Figure 3.11 Theoretical and experimental results of load voltage against load current, for a fixed shaft speed and five excitation conditions

3.5.4 The effect of variations in rotor resistance with temperature upon voltage regulation

Whilst the results shown in figures 3.10 and 3.11 show a good agreement between the model and experiment, it can be seen that in each case the predicted voltage regulation is slightly better than the actual voltage regulation. One reason for this may be increased rotor and stator resistance with load, due to increased operating temperature. This is likely to be much more significant for the rotor resistance because the temperature variation of the rotor with load will be higher than for the stator. For this reason the model was used to illustrate the effect of variations in rotor resistance upon voltage regulation. Figure 3.12 shows experimental results, for 55 μF shunt capacitance and 1500 rpm shaft speed, along with results from the model for three values of rotor resistance. These results indicate that an improvement in the accuracy of the model could be obtained by including resistance variation with temperature.

3.5.5 The effect of unequal stator and rotor leakage reactances upon voltage regulation

In theory, it is possible to separate X_1 and X_2 using the results from the stator resistance test, no-load test, locked rotor test and full load test. However, it is difficult to obtain accurate values, since for the no-load and full load tests the magnetising flux is considerably larger than the leakage flux and is highly dependant upon the magnetising voltage.

In order to determine whether separation of X_1 and X_2 was required the computer model was used to determine the effect of unequal values upon voltage regulation. Figure 3.13 indicates that separation of X_1 and X_2 is not necessary.

3.5.6 The effect of variations of core loss upon voltage regulation

In order to determine the effect of variations in core loss upon voltage regulation, the computer model was run using the calculated value of core loss and then with half and twice the calculated value. As shown in sections 3.3.8 and 3.4.2, the range from half to twice the calculated value of core loss is much greater than the variation that would be expected in practice. The results, shown in figure 3.14, indicate that even with these extreme values the effect on voltage regulation is negligible and confirm that it is reasonable to disregard core loss variation with voltage and frequency.

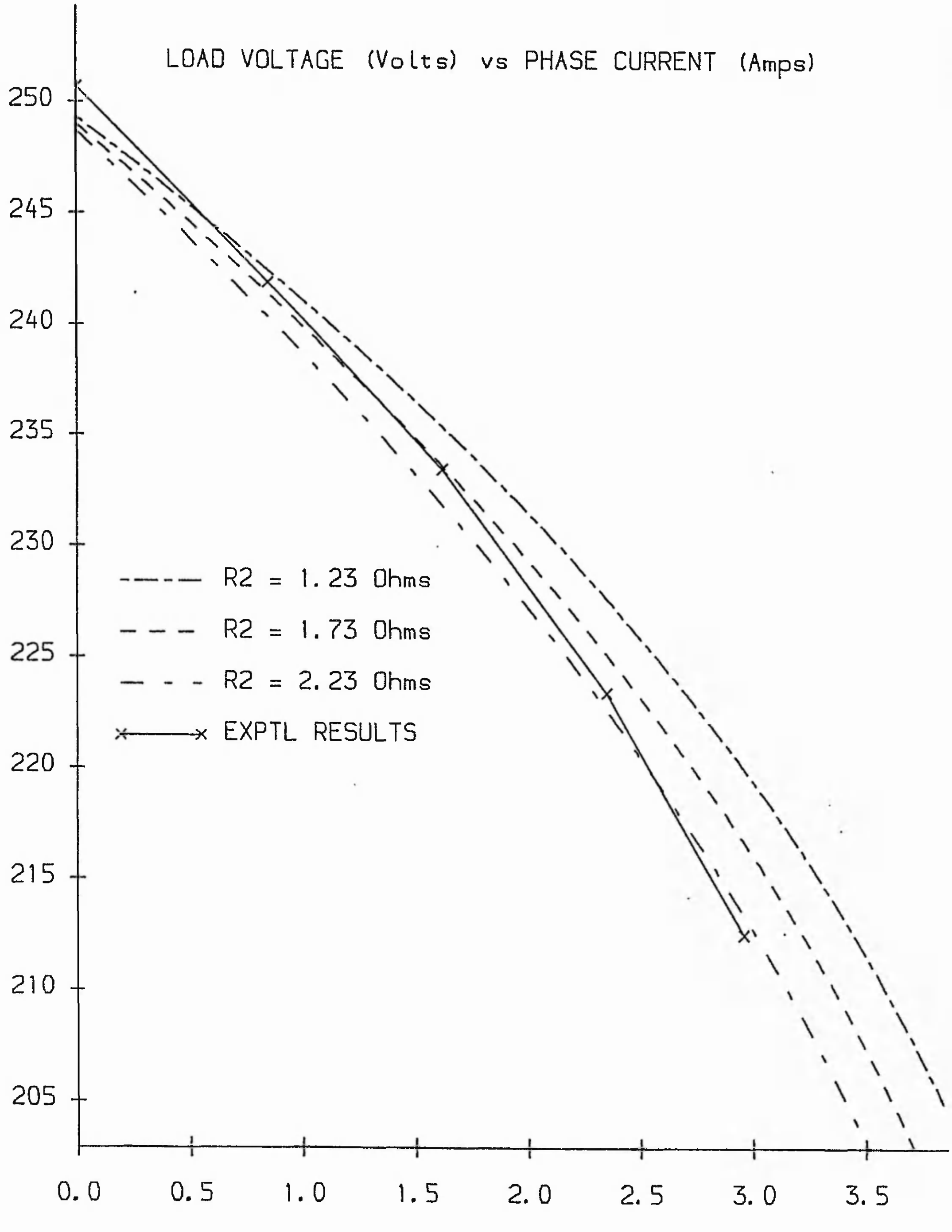


Figure 3.12 The effect of varying rotor resistance upon voltage regulation with load

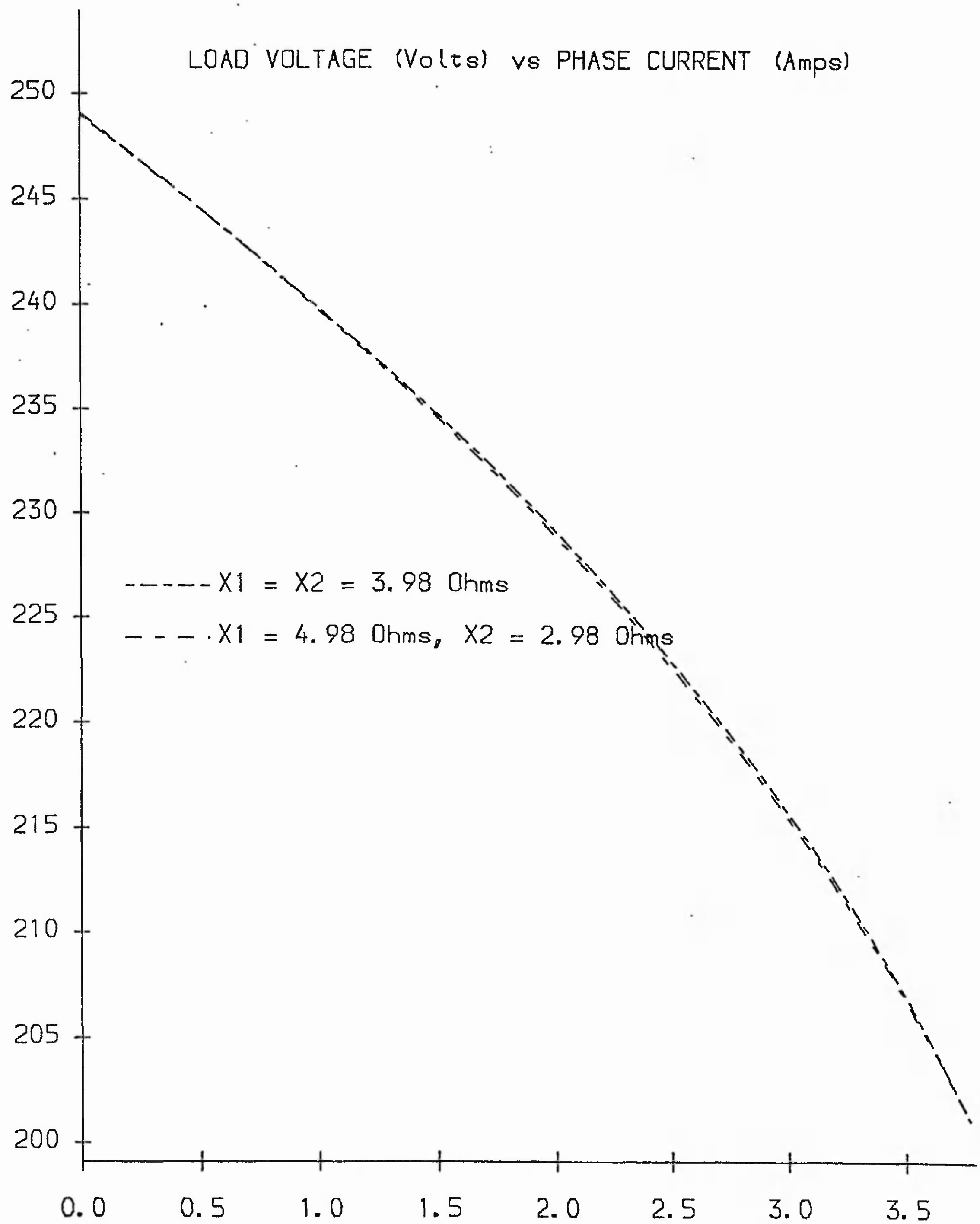


Figure 3.13 The effect of unequal leakage reactances upon voltage regulation with load

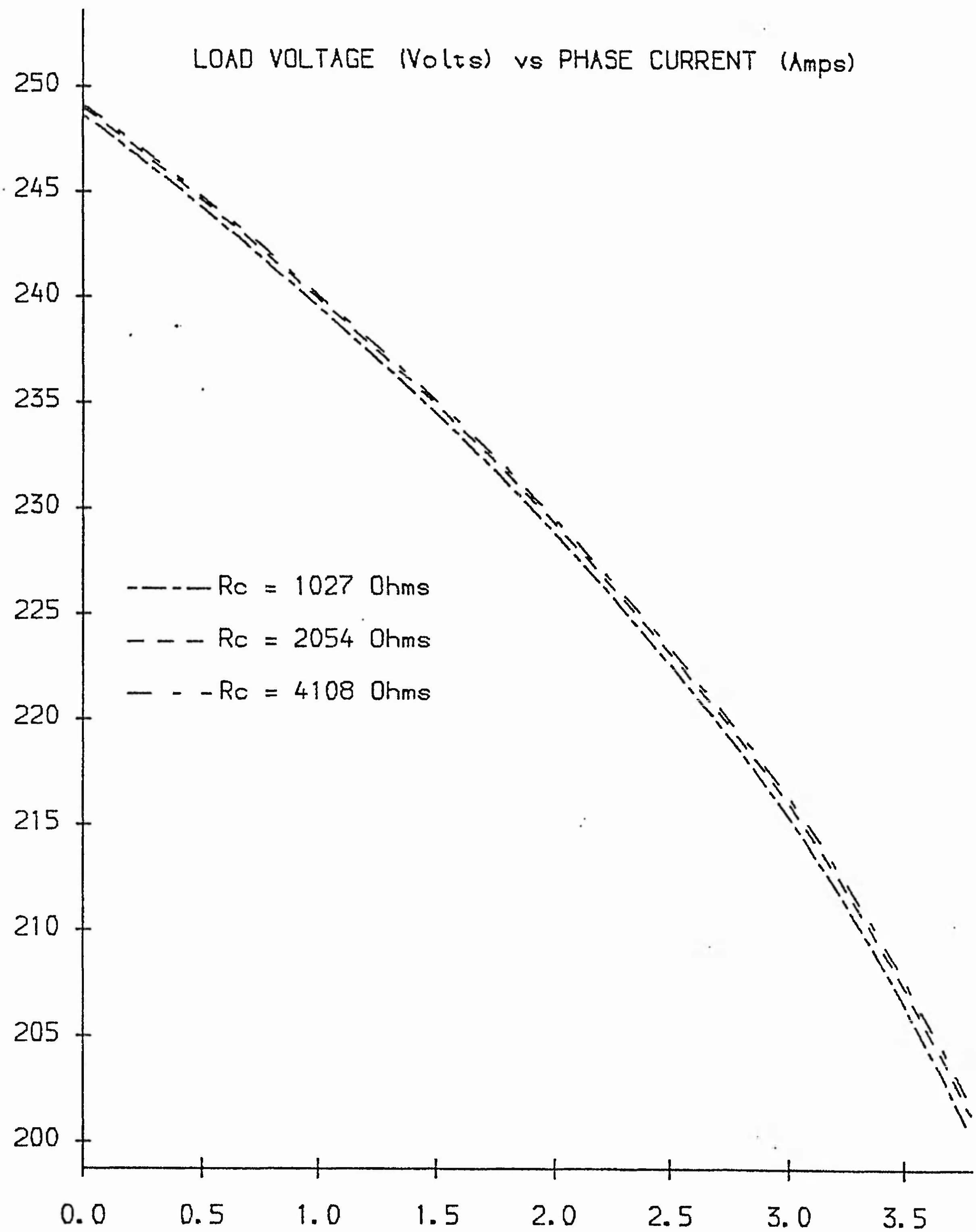


Figure 3.14 The effect of variations in core loss upon voltage regulation with load

3.5.7 Conclusions

The computer model is an accurate representation of the steady-state operation of a stand-alone induction generator over a wide range of excitation and shaft speed conditions. For accurate modelling, separation of the stator and rotor leakage reactances is not required.

The model could be improved by including temperature variations of resistance, though this would require a lot more additional data and would increase the complexity of the model considerably. The model was developed to enable an assessment of the different control approaches outlined in section 2.5. The model is more than adequate to meet this requirement.

CHAPTER 4

ASSESSMENT OF THE NEW CONTROL APPROACHES FOR STAND-ALONE INDUCTION GENERATORS

4.1 INTRODUCTION

In this chapter the three new control system approaches, outlined in section 2.5, are assessed by using the computer model and experimental results. The constant voltage controlled frequency (CVCF) and constant frequency controlled voltage (CFCV) approaches are designed for use with any water turbine, since they do not rely upon the characteristic of the turbine to achieve their regulation. They are designed to provide good voltage regulation for unity and lagging power factor consumer loads.

The controlled voltage and frequency approach (CVF) is designed for use with turbines that have a characteristic such that the turbine power drops off rapidly at overspeed. It is not designed for installations where there is a significant inductive component to the consumer load, unless the loads are power factor corrected.

4.2 THE CONSTANT FREQUENCY CONTROLLED VOLTAGE APPROACH (CFCV)

4.2.1 Introduction

This approach was outlined in section 2.5.2. A standard load controller, with a unity power factor ballast load, is used to maintain a constant frequency. This will also maintain a constant voltage if the consumer load has a unity power factor. Good voltage regulation with varying power factor is achieved by means of capacitance connected in series with the load. As shown, in figures 2.15 and 2.16, this reduces generator saturation for lagging power factor loads. The surplus capacitance then compensates for load inductance.

4.2.2 Results

Results were obtained by using the computer model with the measured parameters for the

2.2 kW machine. Firstly, for comparison purposes, the machine was modelled with no series capacitance connected and with a range of load power factors. The results, for a constant 1500 rpm shaft speed and shunt capacitance per-phase of 55 μF, are shown in figure 4.1. The results when 110 μF of series capacitance is added per-phase are shown in figure 4.2. It is clear by comparing these two sets of results that the addition of series capacitance yields a significant improvement in terms of voltage regulation with inductive load. The main reason for this is the reduction in magnetising voltage that occurs with lagging power factor loads, as shown in figure 4.3.

The use of a load controller will maintain a constant generated frequency and therefore a very near constant load. A better indication of voltage variation with load power factor can be seen from figure 4.4, which displays the same results as figure 4.2 but this time voltage is plotted against load power rather than load current. It can be seen from figure 4.4 that for any load condition, from zero to 700 W per phase, the variation in voltage for the power factor range 1.0 to 0.8 lagging is less than 15 volts. Since the power factor represents the overall load power factor, power dissipated in the ballast load can help to improve the power factor if the consumer load is inductive. In order to confirm the results of the computer model, tests were undertaken for a range of load power factors from 1 to 0.78 lagging, at near rated output. The test results, along with computed points taken from figure 4.4, are given in figure 4.5. The test results agree closely with the model.

It should be noted that load controllers are usually designed to maintain a constant electrical frequency, rather than a constant shaft speed. Because of increasing slip with load the results of figures 4.2, 4.4 and 4.5 do not represent a case of precisely constant frequency. If the frequency were held constant, voltage regulation with load would normally be improved, because saturation increases as frequency falls, adding to the voltage drop with load.

4.2.3 Limitations

The main limitation of series compensation is the instability that may arise when starting motors. From first considerations, series compensation has an advantage in that the large starting current that passes through the series capacitor provides power factor correction for the lagging power factor of the motor. Unfortunately, it can also cause subsynchronous resonance.

The problems of using series capacitance with induction motors have been known for more than 55 years [75]. Upon starting, the rotor of the machine will come up to partial speed and continue to rotate at this reduced speed. The reason for this is that the motor

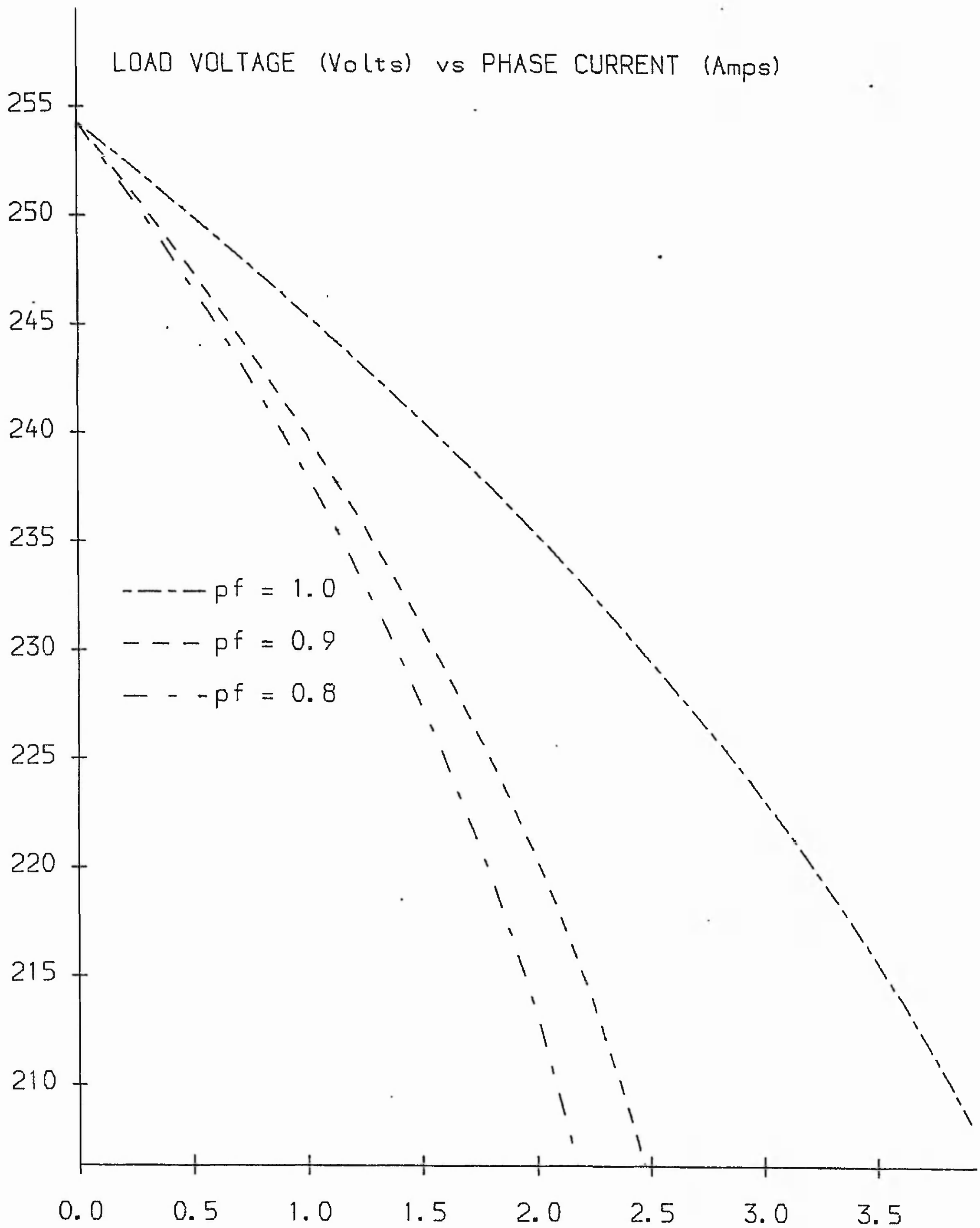


Figure 4.1 Load voltage against load current for shunt excitation alone

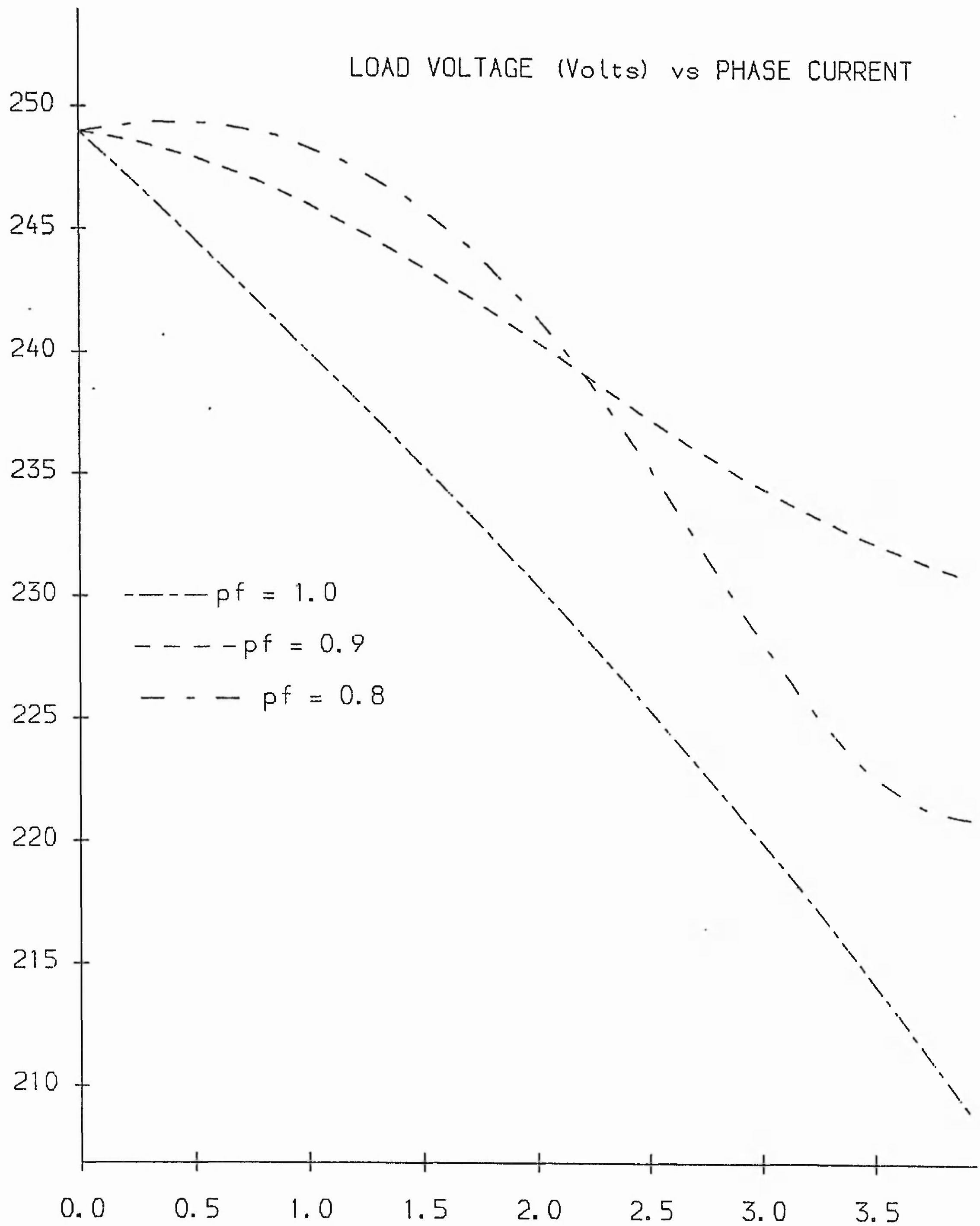


Figure 4.2 Load voltage against load current for combined series and shunt excitation

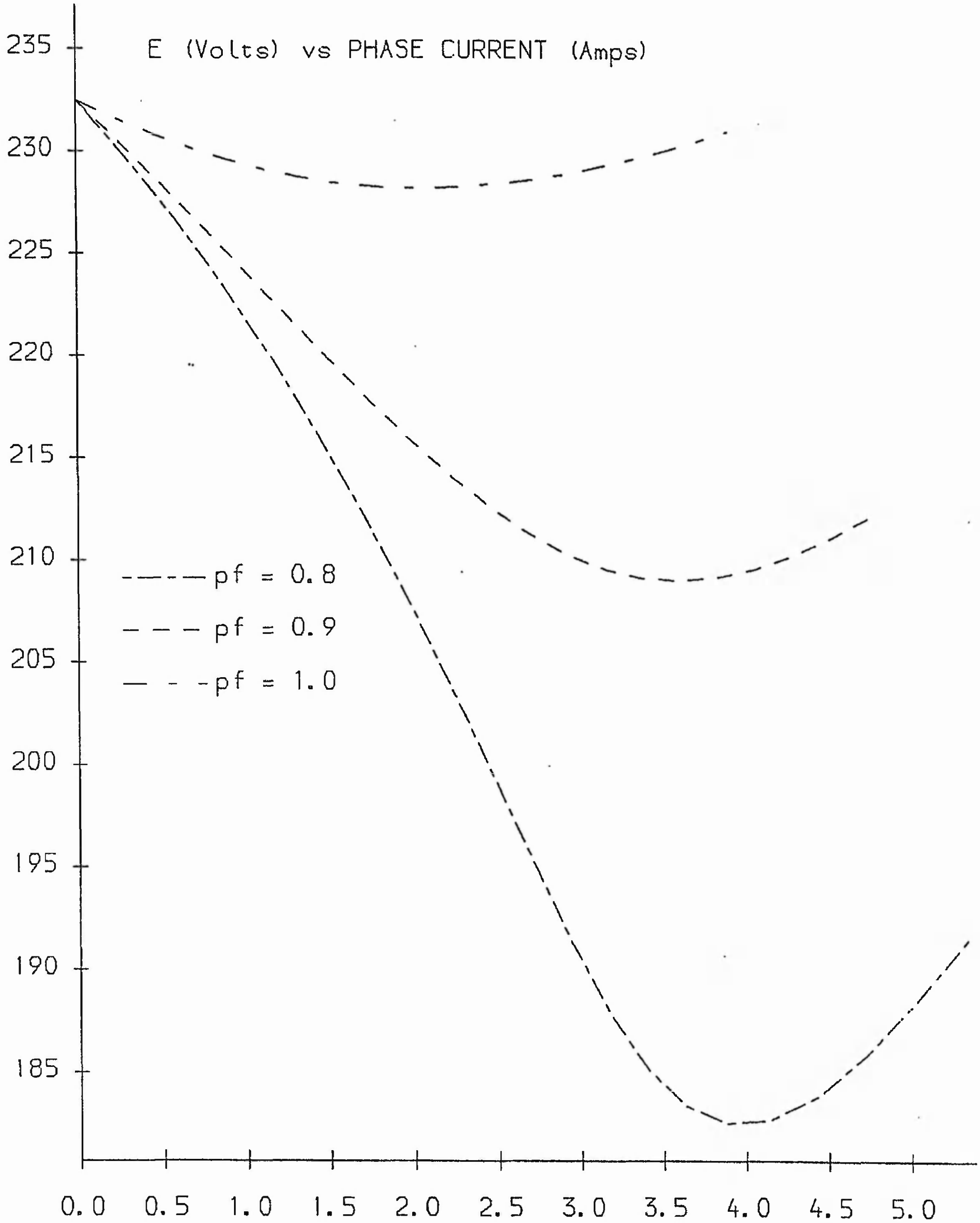


Figure 4.3 Magnetising voltage against load current for combined series and shunt excitation

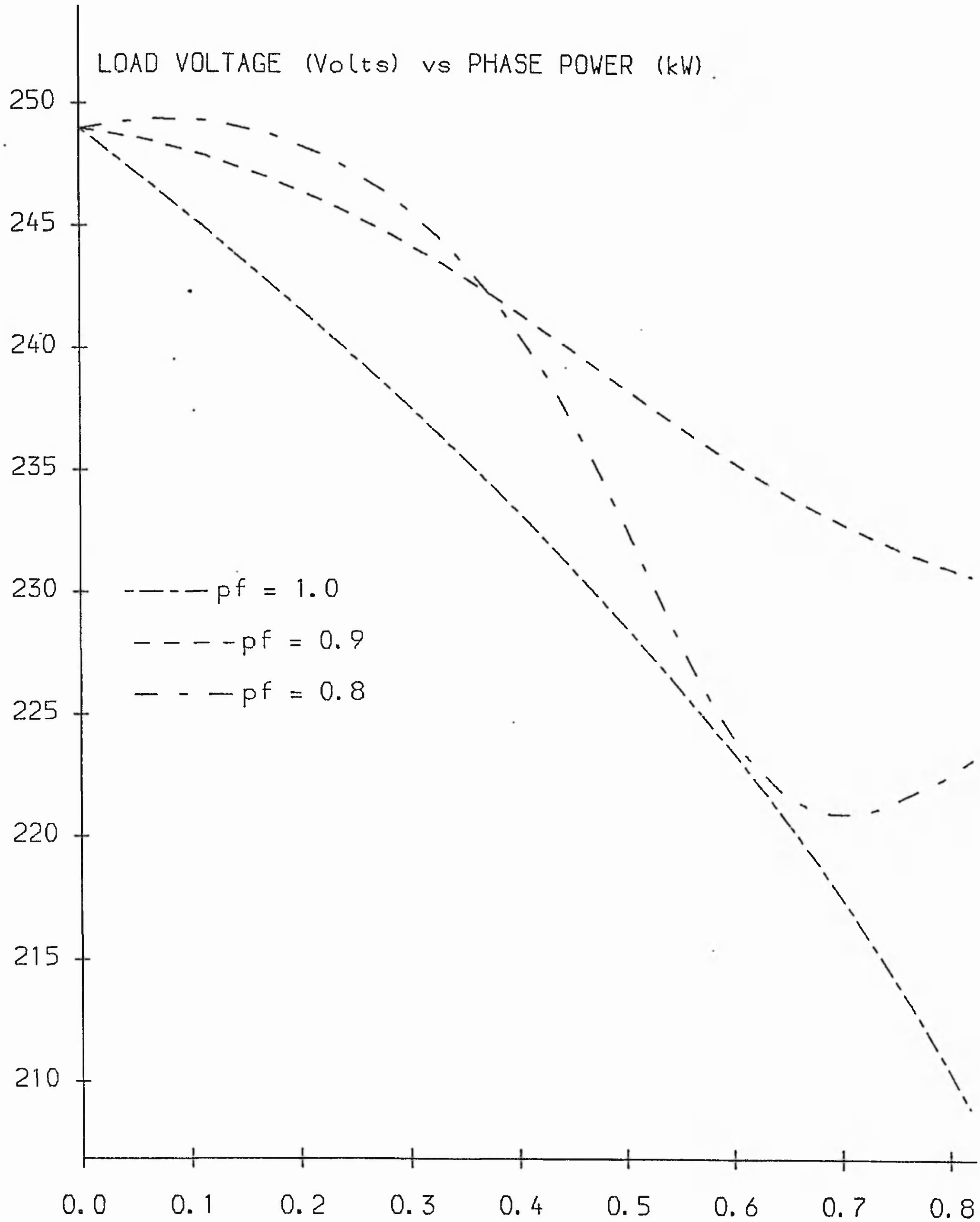


Figure 4.4 Load voltage against phase power for combined series and shunt excitation

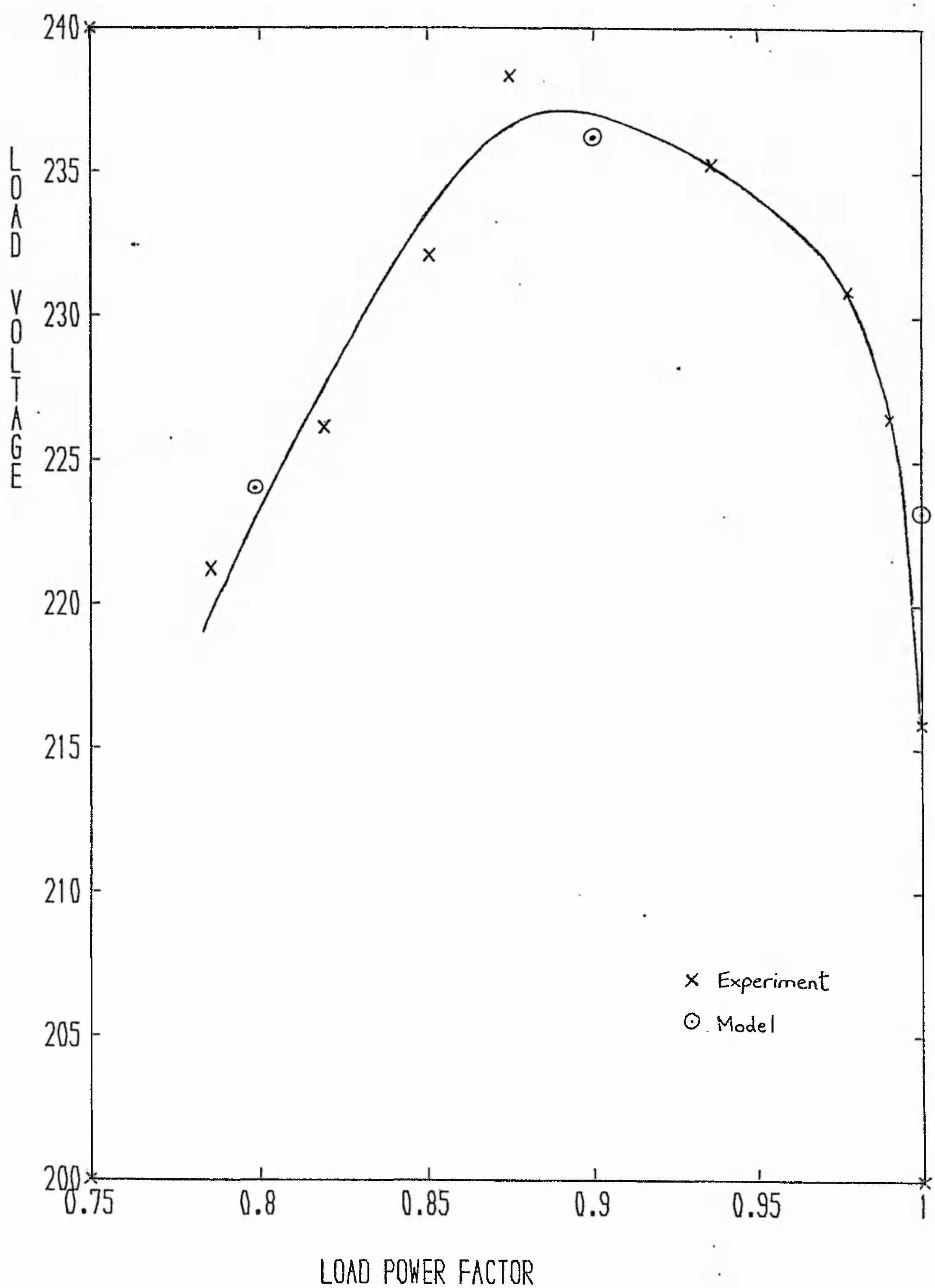


Figure 4.5 Experimental results of load voltage against load power factor at a fixed load of 600 W per phase and with combined shunt and series excitation

acts as a generator with respect to the natural frequency of the circuit, as defined by the series capacitance and inductance [76]. The negative resistance, representing the shaft power, adjusts itself to cancel the effect of the positive resistance within the motor and as a result current flows continuously. The power being supplied, at the generator frequency, that would normally cause the rotor to accelerate is drawn from the shaft by the naturally oscillating circuit. The current flow in the naturally oscillating circuit is limited only by the amount of power that can be drawn from the shaft at the speed necessary to make the resistance of the naturally oscillating circuit zero.

This phenomenon was observed when starting a 4-pole 0.37 kW motor from the 2.2 kW induction motor operating as a generator. The motor reached 920 rpm vibrated wildly and became very hot. The recommended solution to the problem of subsynchronous resonance [77,78,79] is to incorporate damping resistors across the series capacitance. However, this is wasteful and would be a major disadvantage on a micro-hydro system.

An alternative of connecting resistors in series with the motor, to be shorted out once the motor runs up to speed, was tried. However, this was unsuccessful as the resistance required to prevent subsynchronous resonance was too large for sufficient torque to be developed to start the motor. Subsynchronous resonance was not observed with purely shunt capacitve systems but further research is required to fully investigate this problem.

4.3 THE CONSTANT VOLTAGE CONTROLLED FREQUENCY APPROACH (CVCF)

4.3.1 Introduction

This approach was outlined in section 2.5.3. A voltage controller is required, which functions by controlling the power dissipated in a resistive ballast load in order to maintain a constant voltage. Voltage regulation with lagging power loads is achieved by reducing the power dissipated in the ballast load so that the generator speeds up and operates at a higher frequency, thereby reducing the magnetising current and increasing the VARs produced by the capacitors.

4.3.2 Results

Figure 4.6 shows the results from the computer model for the 2.2 kW machine. The results show how compensation for inductive loads is achieved by increasing the speed of the generator. For operation at 600 W per phase, i.e. close to the maximum output, the

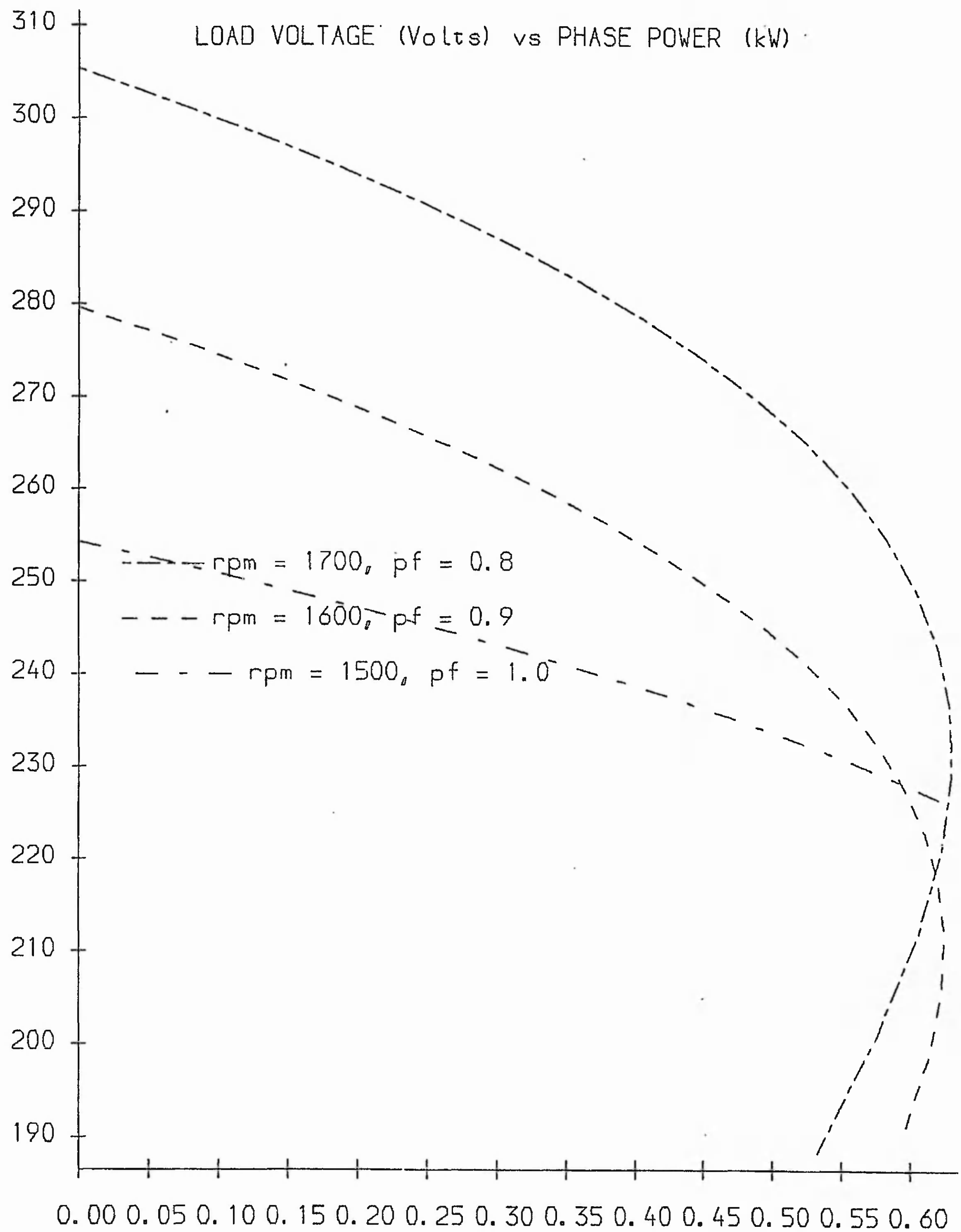


Figure 4.6 Load voltage against load current for shunt excitation and for three values of shaft speed

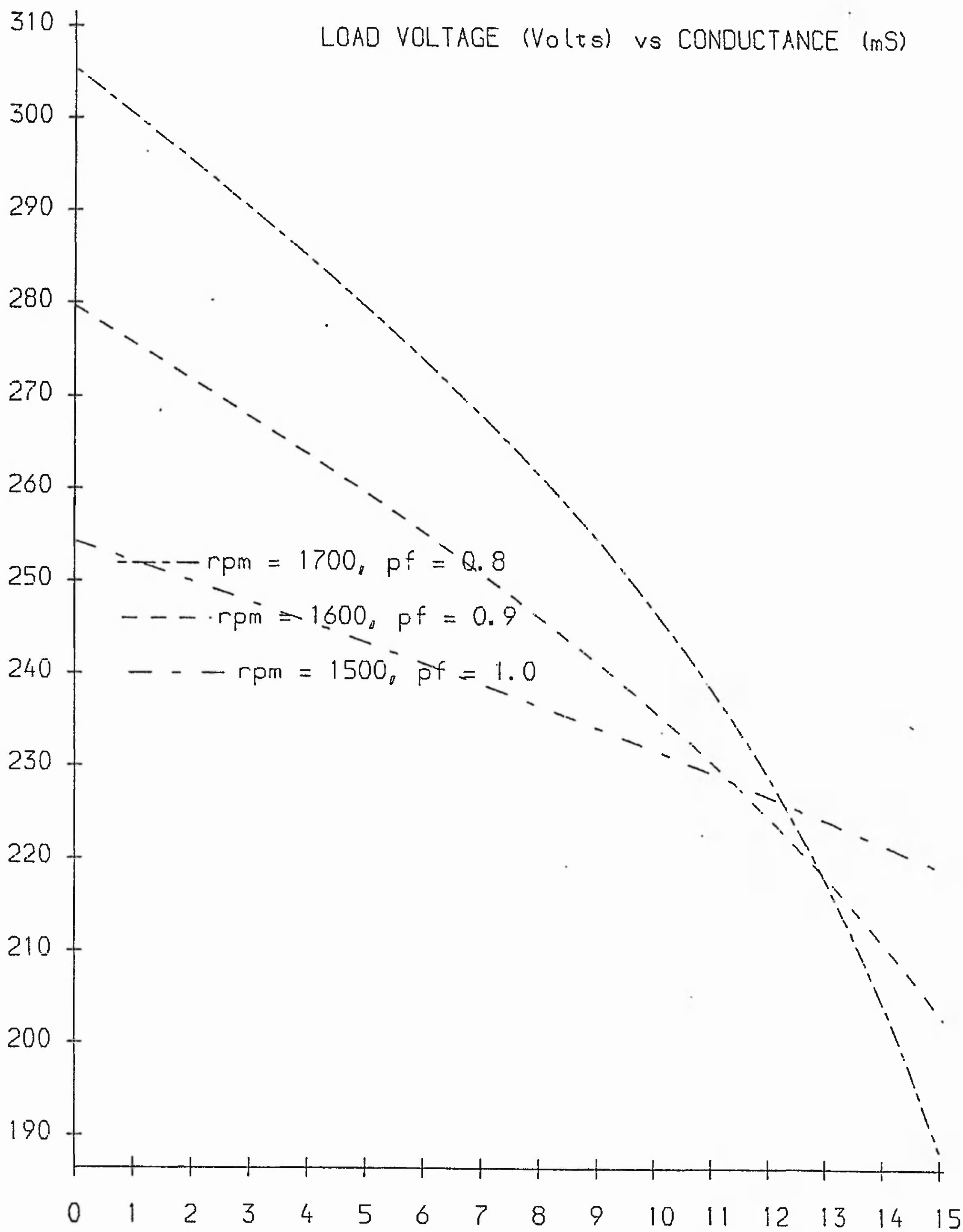


Figure 4.7 Load voltage against load conductance for shunt excitation and for three values of shaft speed

increase in speed required is less than 7% for an overall load power factor of 0.9 and 14% for an overall load power factor of 0.8. 0.8 represents a poor overall power factor as the ballast load is resistive and at higher frequencies power factors generally improve due to reduced saturation.

It would appear from figure 4.6 that instability could occur with large lagging power factor loads because of the very sharp voltage fall off. However, the voltage controller works by varying the load conductance and when the voltage is plotted against this parameter, as shown in figure 4.7, it can be seen that the variation is much more gradual.

4.3.3 Limitations

As explained in section 2.4, overheating can occur with motor loads, whose power requirements increase significantly with speed, if the supply frequency is allowed to rise too much. If such loads are present then, for a system that runs at rated frequency with unity power factor load, the overall load power factor should be maintained at greater than 0.9, by power factor correction if necessary.

Care should also be taken to ensure that increased turbine speed does not significantly reduce power output, due to operation at above the best efficiency speed. This can be easily avoided with impulse turbines and high specific speed reaction turbines as they have relatively flat power speed characteristics at near maximum efficiency. More care is required with low specific speed reaction turbines, such as some pumps as turbines, as their power speed characteristic is more peaked, as shown in the next section.

4.4 THE CONTROLLED VOLTAGE AND FREQUENCY APPROACH (CVF)

4.4.1 Introduction

This approach was outlined in section 2.5.4. It does not involve any form of load control and therefore the turbine speed will decrease as the consumer load increases. The voltage regulation with load is very dependant upon the overspeed characteristic of the turbine. Turbines with runaway speeds only slightly greater than their best efficiency speed will provide the best regulation. Low specific speed pumps as turbines tend to have such characteristics [16]. One such pump was tested in turbine mode and its characteristic was used in the computer model in order to predict its voltage regulation with load.

4.4.2 Results

The experimental arrangement used to measure the pump as turbine characteristics is described in appendix H. A Glim fit was used to model the power speed characteristic and this is shown in figure 4.8. The formula used is given in equation 4.4.1.

$$P_{turb} = P_{max}\{k_0 + k_1\omega + k_{10}\omega^{10}\} \qquad \text{.............4.4.1}$$

Where $k_0 = 0.06$

$k_1 = 3.94 \times 10^{-3}$

$k_{10} = -3.76 \times 10^{-23}$

This characteristic was entered into the model, for a maximum turbine input power of 2.5 kW, and the model was run using the 2.2 kW induction motor as a generator. The results, for four combinations of shunt and series capacitance are shown in figures 4.9 and 4.10. It is clear that the addition of series capacitance helps to improve the voltage regulation with load, though not as significantly as for constant speed conditions.

4.4.3 Limitations

It is apparent from the results of figures 4.9 and 4.10 that the controlled voltage and frequency approach is quite limited as to its range of use, due to its rather poor voltage regulation even with a relatively small speed change. The best results from figures 4.9 and 4.10 are for 30 μF shunt capacitance and 120 μF series capacitance per phase and show a voltage variation from 258 V to 195 V for a speed range from 1715 rpm to 1510 rpm. This voltage range is more than was considered acceptable in section 2.4, even for purely resistive loads. In addition the turbine is always operated at below its best efficiency and this situation would be worse with a turbine with a higher runaway speed.

4.5 CONCLUSIONS

The constant frequency controlled voltage approach (CFCV) provides good frequency regulation and acceptable voltage regulation. However, it has three main drawbacks:-

1) In order to obtain good voltage regulation the values of shunt and series capacitance must be carefully selected.

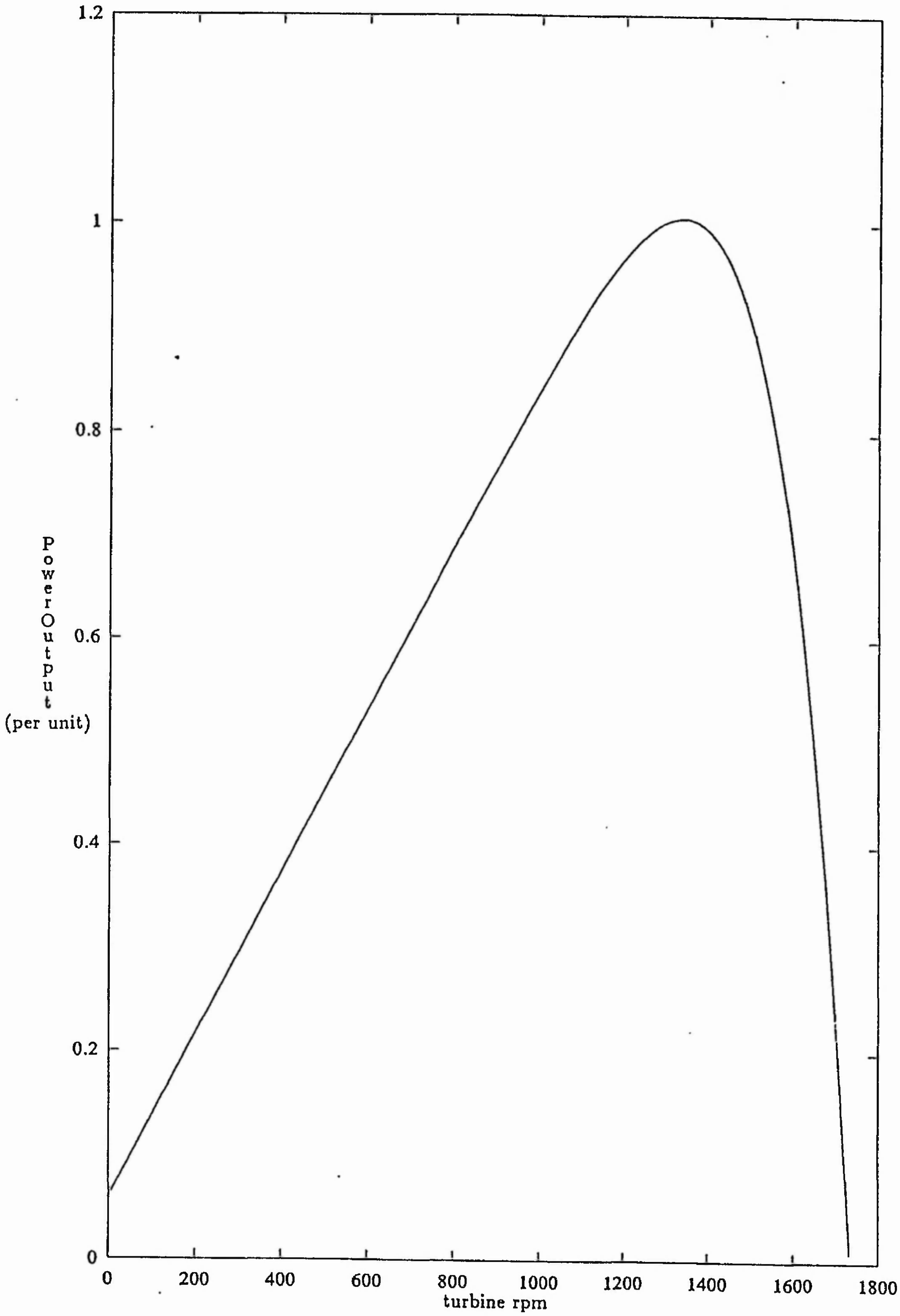


Figure 4.8 GLIM fit of power-speed characteristic for the pump as turbine

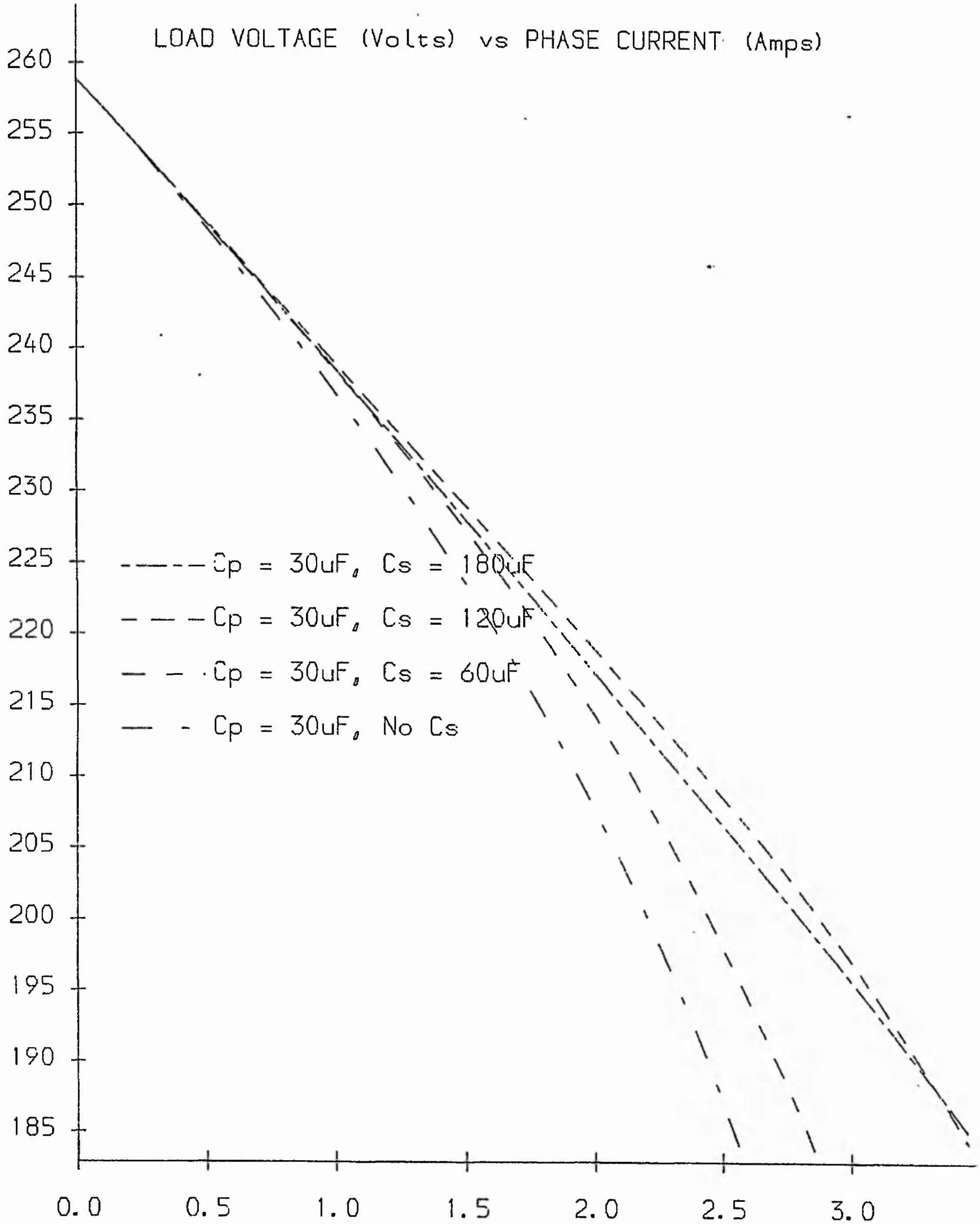


Figure 4.9 Load voltage against load current for pump unit running as turbine-generator

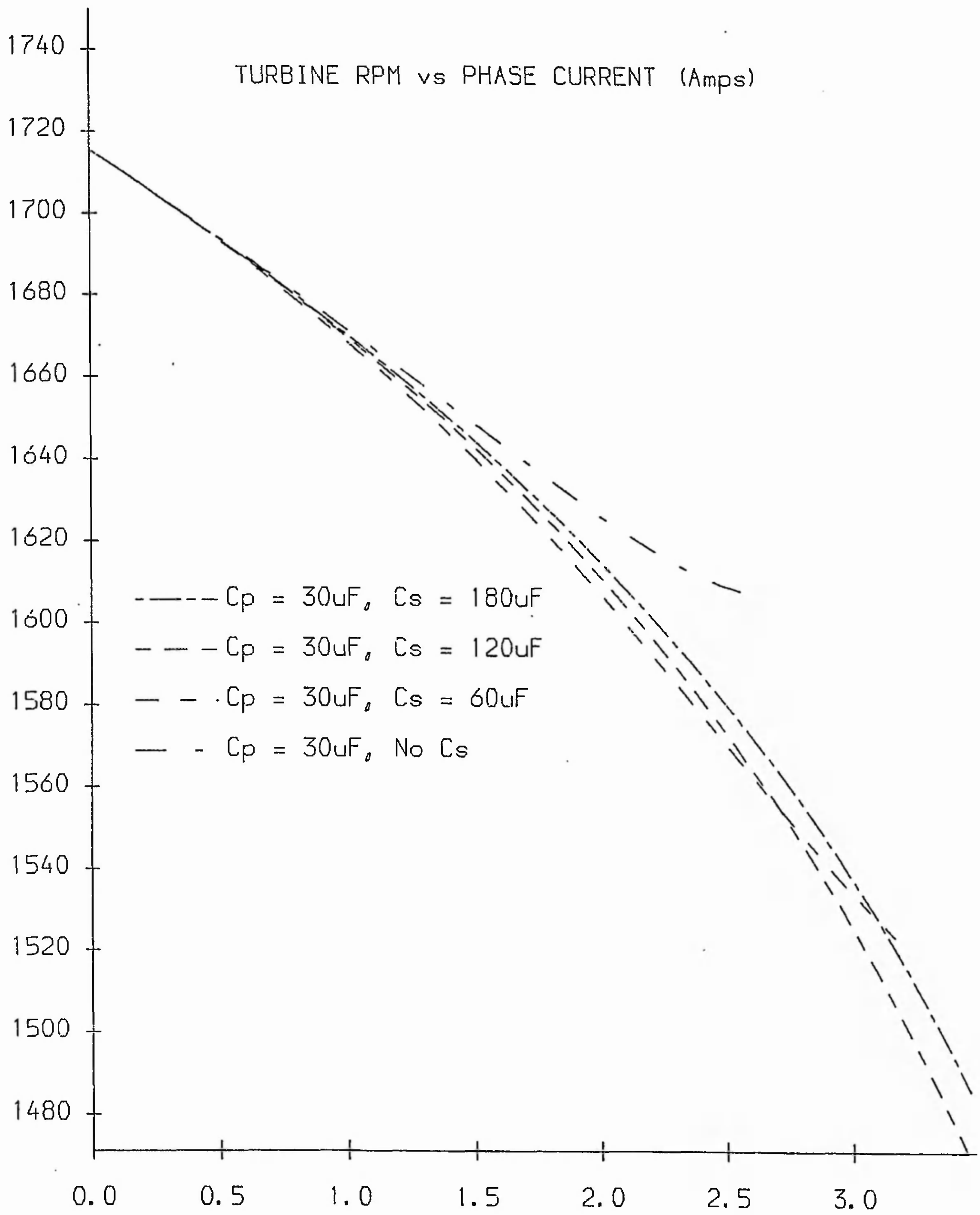


Figure 4.10 Shaft speed against load current for pump unit running as turbine-generator

2) The capacitor cost will be high because the series capacitors will cost nearly as much per micro-Farad as the shunt capacitors. This is because, even though the voltage rating of the series capacitors is less than that of the shunt capacitors, power capacitors are rarely available for voltages of less than 220/240 Volts.

3) The problem of subsynchronous resonance will prevent induction motors from being used in many cases.

The constant voltage controlled frequency approach (CVCF) provides good voltage regulation and acceptable frequency regulation. It does not suffer from the drawbacks listed for the CFCV approach for the following reasons:-

1) Shunt capacitance selection is relatively straightforward as it can be closely estimated from the power factor of the motor or data for similar motors. Any error can be corrected for by allowing a small change in operating speed, which is achieved naturally by means of the voltage controller.

2) The cost of the capacitors is relatively low because no series capacitance is required.

3) Subsynchronous resonance has not been observed or reported for motor starts on induction generators with only shunt capacitance connected.

The main drawback of the CVCF approach is operation with motor loads, whose power requirements increase significantly with speed. This can be overcome by careful power factor correction of significant inductive loads. The voltage controller for the CVCF approach will be very similar in terms of cost and complexity to the frequency contoller for the CFCV approach, since the only difference is the parameter that is measured.

The controlled voltage and frequency approach (CVF) is simple and cheap but will be of very limited use because of its poor voltage regulation, even with turbines whose power output falls off rapidly at overspeed.

The most appropriate of the three control options considered is the constant voltage controlled frequency approach. This was adopted as the approach to be implemented and to this end a voltage controller was developed, as described in the next chapter.

CHAPTER 5

THE INDUCTION GENERATOR CONTROLLER (IGC)

5.1 INTRODUCTION

It is clear, from chapter four, that the constant voltage controlled frequency method is the most suitable approach for controlling stand-alone induction generators. In order to minimise frequency variation the power factor produced by the controller should be as close to unity as possible.

As explained in section 2.2.2, a phase-angle control approach will produce a variable lagging power factor and is therefore not very suitable for controlling induction generators. Two methods were considered; the first using a binary-weighted load arrangement and the second a variable mark-space ratio chopper. Controllers of both types were designed, built and tested both in the laboratory and in the field.

5.2 BINARY-WEIGHTED IGC

5.2.1 Introduction

The basic principle of binary-weighted controllers is given in section 2.2.2. A number of controllers that use this method have been designed as electronic load controllers for synchronous generators [44,45,46]. Whilst electronic load controllers sense and control frequency, the IGC senses and controls voltage.

The generator voltage cannot be controlled to a fixed value because a step change in ballast load will produce a step change of voltage. Instead of controlling the voltage to a fixed value the voltage is kept between upper and lower limits which define a 'voltage window'. When the generated voltage is within this voltage window the ballast load remains unchanged. When it is above the upper limit the ballast load is incremented and when it is below the lower limit it is decremented.

5.2.2 Analysis of the voltage window

The minimum size of the voltage window is determined by how much the generator voltage varies when the least significant ballast load is switched on and off. If the window is made smaller than this value then under certain load conditions the ballast will continually vary, resulting in a voltage fluctuation which is likely to cause flickering of light bulbs. Such instability is unacceptable.

The minimum voltage window depends upon the operating conditions. To illustrate this, four cases will be considered, represented by the operating points A,B,C and D on figure 5.1. For each case a 15 kW ballast load, consisting of four binary-weighted loads will be assumed. The minimum load step is 1 kW and the load values are 8 kW, 4 kW, 2 kW and 1 kW. The losses in the generator and drive are ignored.

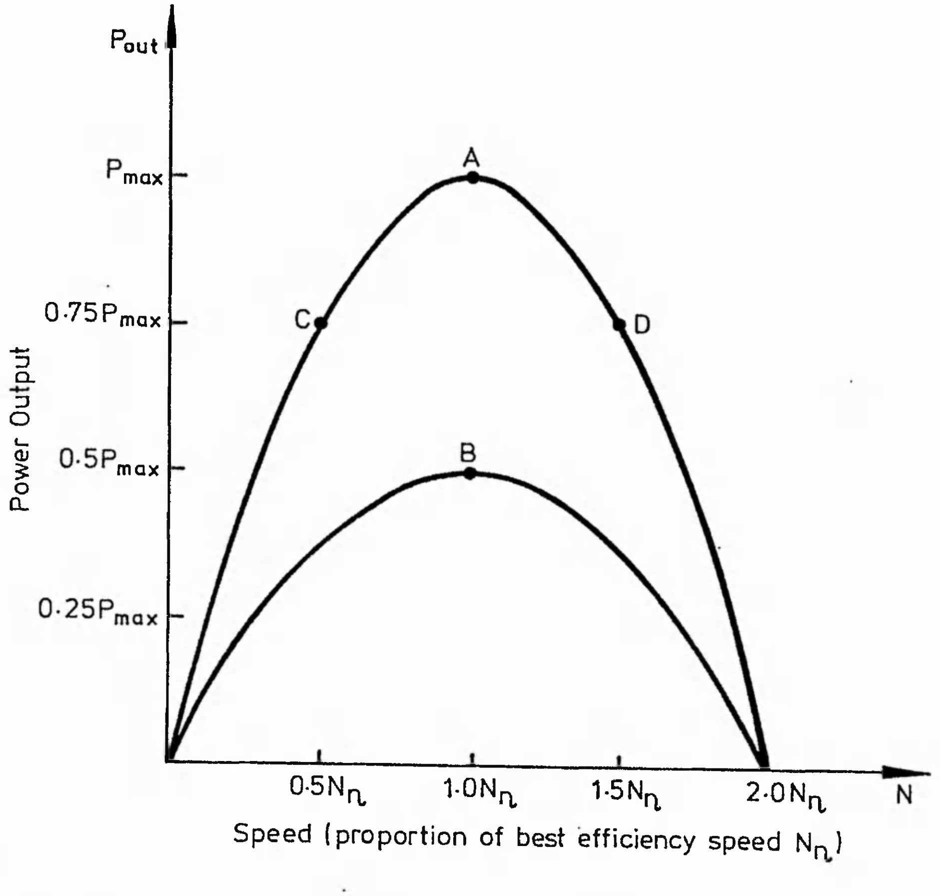


Figure 5.1 Typical turbine power speed characteristics at full and half power

A) Operation with the turbine running at its best efficiency speed and at full load output.

As the turbine power output curve is flat at point A the power will remain constant for small variations in speed. With no consumer load connected, the effect of removing one step of ballast load will be to cause the voltage to rise until 15 kW is dissipated in a ballast with loads designed for 14 kW at rated voltage. This can be simply calculated from:-

$$\frac{V_{new}^2}{R/14} = \frac{V_{old}^2}{R/15} \qquad \text{..............5.2.1}$$

where R is the resistance of a 1 kW load at its rated voltage
V_{old} is the voltage before the ballast is reduced
V_{new} is the voltage after the ballast has been been reduced

$$V_{new} = V_{old}\sqrt{15/14} \qquad \text{..............5.2.2}$$

$$V_{new} = 1.035\ V_{old} \qquad \text{..............5.2.3}$$

In this case, the voltage window must be greater than 3.5% of the lower voltage limit.

B) Operation with the turbine running at its best efficiency speed but at only half its maximum output

This condition could be due to seasonal or occasional low flow conditions or because the ballast is oversized. The power output is 7.5 kW and hence the maximum ballast load that can be connected is 7 steps, or 7 kW at its rated voltage. Assuming that sufficient user load is connected for the voltage to be at the lower limit of the voltage window then, using equation 5.2.2, if the ballast is reduced by one step,

$$V_{new} = V_{old}\sqrt{7.5/6.5} \qquad \text{..............5.2.4}$$

$$V_{new} = 1.074\ V_{old} \qquad \text{..............5.2.5}$$

In this case, the voltage window must be greater than 7.4% of the lower voltage limit.

C) Operation with the turbine running at below its best efficiency speed

When the load is reduced by removal of a step of load, the load on the turbine is reduced, resulting in an increase in turbine speed. In this case the turbine output power increases with speed, adding to the initial difference. Therefore, the voltage must increase by an additional amount to compensate for the change in the power output of the turbine.

An indication of the voltage variation with speed for induction generators can be gained from the test results shown in figure 3.10. These results show that for a fractional increase in speed α, where α equals $\Delta N/N$, there is an increase in voltage of approximately 1.5α.

$$\text{Hence} \quad V_{new} = V_{old}\left(1 + \frac{3\alpha}{2}\right) \qquad \text{..............5.2.6}$$

The turbine characteristic of figure 5.1 is for an ideal impulse turbine and therefore the power output P_{out} is determined by the following formula:-

$$P_{out} = \frac{N\left(2N_\eta - N\right)}{N_\eta^{2}} P_{max} \qquad \text{..............5.2.7}$$

Where $N\eta$ is the speed for maximum efficiency

Differentiating with respect to speed gives,

$$\frac{dP_{out}}{dN} = \frac{2N_\eta - 2N}{N_\eta^{2}} P_{max} \qquad \text{..............5.2.8}$$

For small changes in N,

$$\Delta P_{out} \approx \frac{2N_\eta - 2N}{N_\eta^{2}} P_{max}\, \Delta N \qquad \text{..............5.2.9}$$

For operation at point C, on figure 5.1, $N = N\eta/2$ and therefore

$$\Delta P_{out} = \frac{\Delta N}{2N} P_{max} \qquad \text{..............5.2.10}$$

Hence, for a fractional increase in speed α there is an increase in turbine power output of,

$$\Delta P_{out} = \frac{\alpha}{2} P_{max} \qquad \text{..............5.2.11}$$

The actual power output at point C, P_c, is 0.75 P_{max} and therefore the increase in turbine output with speed at point C is

$$\Delta P_{out} = \frac{2\alpha}{3} P_c \qquad \text{..............5.2.12}$$

For a one step reduction in ballast, using equation 5.2.1,

$$\frac{V_{new}^{2}}{R/(10.25)} = \frac{V_{old}^{2}\left(1 + \frac{2\alpha}{3}\right)}{R/(11.25)} \qquad \text{..............5.2.13}$$

Substituting for V_{new} using equation 5.2.6,

$$\frac{V_{old}^{2}\left(1 + \frac{3\alpha}{2}\right)^{2}}{R/(10.25)} = \frac{V_{old}^{2}\left(1 + \frac{2\alpha}{3}\right)}{R/(11.25)} \qquad \text{..............5.2.14}$$

Simplifying this equation,

$$10.25\left(1 + \frac{3\alpha}{2}\right)^{2} = 11.25\left(1 + \frac{2\alpha}{3}\right) \qquad \text{..............5.2.15}$$

Solving this quadratic equation, and using the result to calulate V_{new}

$$V_{new} = 1.062\ V_{old} \qquad \text{..............5.2.16}$$

D) Operation with the turbine running at above its best efficiency speed

Using the same approach as used in the previous section equation 5.2.12 becomes

$$\Delta P_{out} = 2\propto P_d \qquad \text{..............5.2.17}$$

Where P_d is the power output at point D on figure 5.1

and $$V_{new} = 1.028\ V_{old} \qquad \text{..............5.2.18}$$

Conclusions

Two important conclusions can be drawn from these examples. Firstly it is important to match the ballast load to the output power of the generator. If the ballast load is larger than the output power of the generator then the voltage variation will be increased. Secondly, from voltage regulation considerations, operation at a speed that is higher than the best efficiency speed is better than operation at below the best efficiency speed.

The analysis has been performed with the assumption that the ballast loads are exactly binary weighted. This will not be true in practice as, even if it were possible to obtain precise matching of the loads when cold, resistance variation with temperature will affect the matching when the controller is operating.

The minimum size of the voltage window will be affected to a small extent by variations in generator efficiency caused by the voltage and frequency changes. This will be dependant on the parameters of the machine and will be more significant with smaller machines due to their lower efficiency and greater saturation.

5.2.3 Basic controller design

The binary-weighted IGC functions by comparing the supply voltage, with upper and lower voltage limits. These voltage limits are variable and are set by means of two comparators. The supply voltage is first transformed down to a suitable level, and then rectified and passed through a peak detector.

If the supply voltage is within the voltage window then the ballast load is not changed. If it is greater than the upper limit, and the ballast load is not already at maximum, then the ballast load is incremented by one step. If it is below the lower limit, and the ballast is not already at minimum, then the ballast load is decremented by one step. The rate at which the ballast can be incremented or decremented is adjustable to allow the controller to be tuned to match the characteristics of the turbine-generator system. The upper and lower voltage limits are also adjustable.

The truth table, used to determine whether the ballast should be increased or decreased, is shown in table 5.1.

5.2.4 Consumer load protection circuitry

It was decided to build basic consumer load protection into the IGC for a number of reasons:-

1) To ensures that a reasonable level of consumer protection is installed.

2) It is cheaper than adding separate protection, since a power supply and voltage sensing circuit are already present.

3) The protection can be easily designed so that it does not interfere with the normal operation of the controller and is not interfered with by the controller.

The protection has been designed to disconnect all of the consumers if an overvoltage or long term undervoltage occurs. Short-term undervoltages may occur due to motor starts and therefore the controller was designed so that under these conditions the consumer load remains connected.

The most important protection required is against overvoltages. This must be fast acting in order to prevent damage to the consumer loads. An additional comparator, D in the circuit diagram shown in figure 5.2, was added to determine if an overvoltage had occured.

The sustained undervoltage trip, which is primarily required to protect motors, was designed so that if the voltage fell below the lower voltage limit for a set number of clock periods then the consumer would be disconnected. A reset switch was incorporated to

Comp A	QD_n	QC_n	QB_n	QA_n	QD_{n+1}	QC_{n+1}	QB_{n+1}	QA_{n+1}
0	0	0	0	0	0	0	0	0
0	0	0	0	1	0	0	0	0
0	0	0	1	0	0	0	0	1
0	0	0	1	1	0	0	1	0
0	0	1	0	0	0	0	1	1
0	0	1	0	1	0	1	0	0
0	0	1	1	0	0	1	0	1
0	0	1	1	1	0	1	1	0
0	1	0	0	0	0	1	1	1
0	1	0	0	1	1	0	0	0
0	1	0	1	0	1	0	0	1
0	1	0	1	1	1	0	1	0
0	1	1	0	0	1	0	1	1
0	1	1	0	1	1	1	0	0
0	1	1	1	0	1	1	0	1
0	1	1	1	1	1	1	1	0
1	0	0	0	0	0	0	0	1
1	0	0	0	1	0	0	1	0
1	0	0	1	0	0	0	1	1
1	0	0	1	1	0	1	0	0
1	0	1	0	0	0	1	0	1
1	0	1	0	1	0	1	1	0
1	0	1	1	0	0	1	1	1
1	0	1	1	1	1	0	0	0
1	1	0	0	0	1	0	0	1
1	1	0	0	1	1	0	1	0
1	1	0	1	0	1	0	1	1
1	1	0	1	1	1	1	0	0
1	1	1	0	0	1	1	0	1
1	1	1	0	1	1	1	1	0
1	1	1	1	0	1	1	1	1
1	1	1	1	1	1	1	1	1

Table 5.1 Truth table for binary-weighted controller

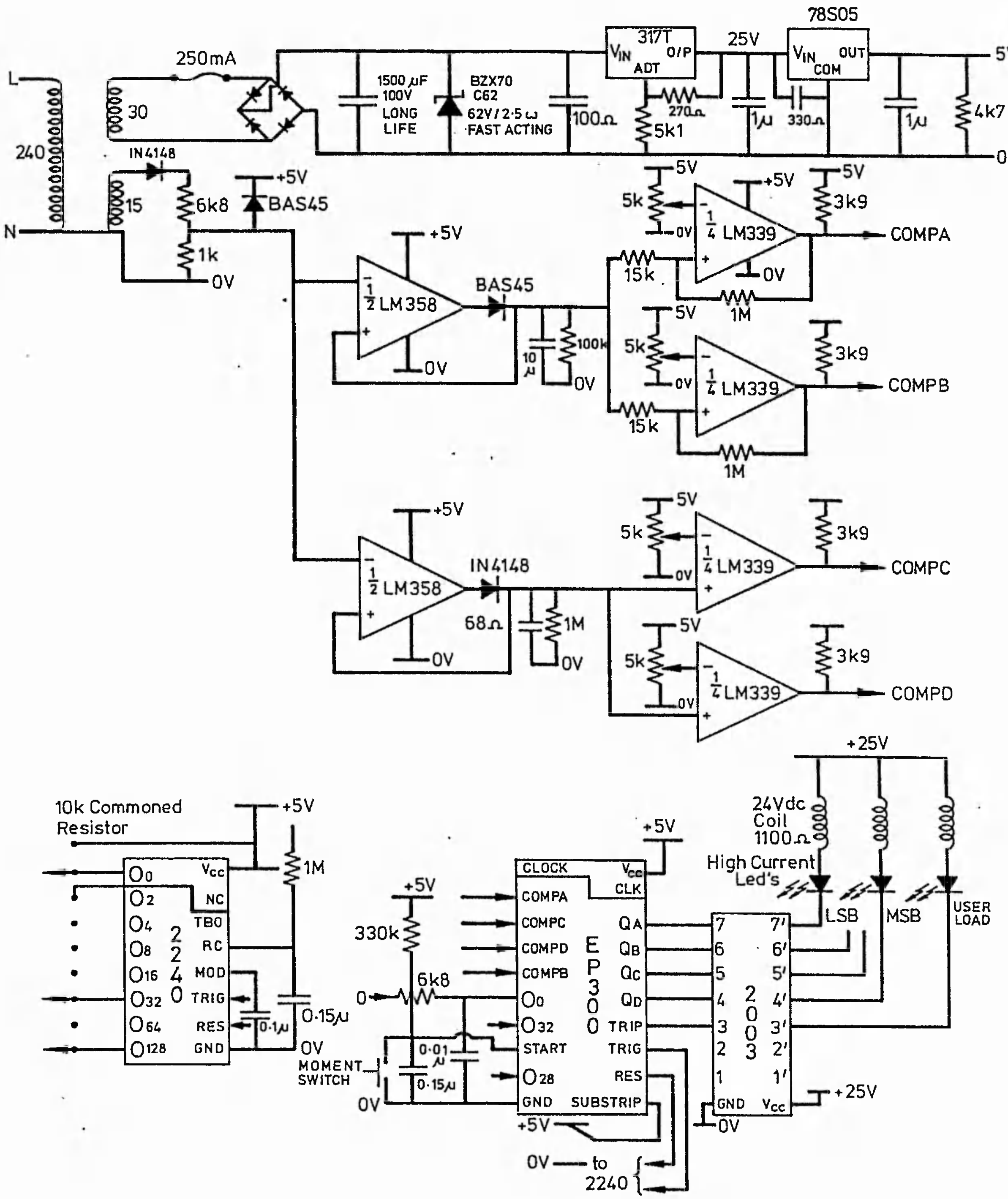


Figure 5.2 Circuit diagram of binary-weighted IGC

allow the consumer load to be reconnected.

A fourth comparator, C in the circuit diagram, was added to aid motor starting by immediately switching out all the ballast loads when the voltage falls below a certain level, which is adjustable.

5.2.5 Detailed controller design

The important considerations for the detailed design of the controller were reliability, cost and ease of manufacture.

Power supply

Special attention was paid to the design of the power supply as it must be able to operate over a wide range of voltage and frequency conditions. Standard power supply transformers should not be used at below their rated frequency or above their their rated voltage, as the resulting increase in magnetising current is likely to cause the primary winding to burn out due to excessive heating. Modern power supply transformers tend to be highly saturated and have very little safety margin to allow for variations in the supply.

To ensure that the transformer will operate at reduced supply frequency and/or increased supply voltage a purpose wound transformer with low magnetising current at rated voltage and frequency was used. This was made using a transformer kit, with ready wound primary. Saturation was reduced by winding an additional primary and connecting this in series with the existing winding. The additional primary was wound with half the turns of the original primary so that at rated frequency the voltage could be increased by 50% without fear of burning out the primary. Such a large increase in the generated voltage can only be achieved with an increase in frequency. The transformer magnetising current falls with increasing frequency and hence it will withstand an overvoltage of significantly more than 50% without fear of damage. The fuses or miniature circuit breakers required to protect the excitation capacitors can easily be chosen so that they will operate before the transformer is damaged. By disconnecting the capacitors the generator will de-excite and thereby protect the power supply.

In order to reduce the complexity of the power supply, the control circuit was designed using i.c.s that can be operated from a single 5 Volt power supply. An additional 25 Volt power supply was required to supply the relay coils that are used to switch in the ballasts and the consumer supply. A voltage regulator that can withstand a high input voltage, 65 Volts in this case, was chosen to ensure reliable operation under overvoltage conditions.

The 5 Volt supply is derived from the 25 Volt supply using a standard voltage regulator. The 5 Volt supply is established at a very low supply voltage, typically 50 V_{rms}. Below this supply voltage the electronic control circuitry may not function according to the design. However, this is of no consequence as under these circumstances the voltage is insufficient to energise the relay coils and therefore the ballast loads cannot be connected.

Voltage sensing

An additional secondary winding was wound on the transformer for voltage sensing. The winding used for the power supply was not suitable as the non-sinusoidal current drawn by the reservoir capacitor distorts the voltage waveform. For cheapness and simplicity the voltage was measured using a peak detecting circuit. This type of detection is affected by harmonic distortion. However, since the operation of the controller maintains a near constant voltage, frequency and generator load the waveform distortion will vary little under normal operating conditions.

The discharge of the peak detector capacitor produces a voltage ripple on the signal that is compared with the upper and lower limits of the voltage window. To compensate for this effect the size of the window must be increased. This results in poorer voltage regulation. However, if the rate of discharge of the peak detector capacitor is reduced, so as to reduce the ripple, then the response time of the controller to a drop in the supply voltage will be increased. Rather than compromising between these two opposing requirements two peak detectors were used. The first has a time constant of 1 second and is used to determine whether the ballast should be increased or decreased under normal operation. The second has a time constant of 68 milli-seconds and is used to detect rapid changes in voltage. It is used to rapidly switch out all the ballasts in the event of a large load, such as a motor, being connected. This peak detector is also used for overvoltage detection. The first peak detector also responds rapidly to a voltage increase, but its slow discharge could cause voltage spikes to be mistaken for sustained overvoltages.

Digital Electronics

The design was first built using standard CMOS i.c.s. However, eight i.c.s were required and although cheapest in terms of component costs the price increases when the size of the circuit board and box are considered. For this reason a programmable logic array was used along with a timer chip with inbuilt counter. The additional cost of the chips was only £3. The program for the logic array is given in appendix J.

A disadvantage of this approach for developing countries, was the problem of obtaining and programming the logic array. This prompted Ben Van Wijhe in Nepal to rework the design using components that could be easily obtained locally [80]. By not including any overvoltage or undervoltage protection in the controller and by a clever timer design he was able to build his controller with just two digital i.c.s.

Relays

The controller was designed so that it could drive either electro-mechanical relays or solid-state relays to control the ballast and user load. Electro-mechanical relays are cheaper than solid-state relays and do not require heatsinks. However, solid-state relays are more reliable provided that they are protected against short-circuits and voltage transients. Also they can be driven directly from CMOS or TTL i.c.s due to their lower current requirement.

The original controllers were built with electro-mechanical relays. Controllers built in Nepal have used solid-state relays because of difficulty with obtaining good quality electro-mechanical relays.

5.3 MARK-SPACE RATIO IGC

5.3.1 Introduction

Whereas the binary weighted controller uses a number of ballast loads and controls the voltage within a 'voltage window', the mark-space ratio controller, in its simplest form, requires just a single ballast load and maintains the voltage at a near constant value irrespective of the generator output power.

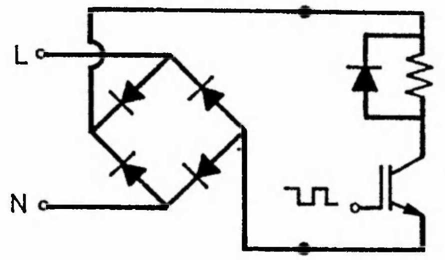


Figure 5.3 Basic switching circuit for single-phase mark-space ratio controller

The basic switching circuit for a single-phase controller is shown in figure 5.3. The ballast load is connected across the rectified output of the generator and switched on and off with a variable mark-space ratio. The on-time can be varied over the full range of 0% to 100%, and is controlled so as to maintain a constant generated voltage. A switching frequency of 1 kHz is used. A higher frequency would reduce waveform distortion but increase switching losses in the transistor. Recorded voltage waveforms are shown in figure 5.4.

A full manual on the design and operation of the controller was produced for a training course. This is given in appendix K.

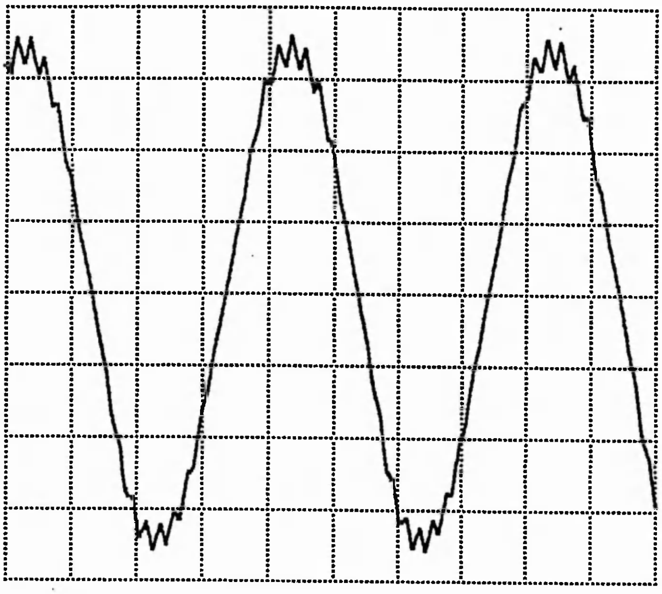
5.3.2 Basic controller design

A block diagram of the controller is shown in figure 5.5. The voltage sensing circuit takes its signal from the output of the bridge rectifier. This is attenuated and filtered so as to reduce the effects of any switching spikes that are present. The resulting signal is fed into a peak detector. The output of the peak detector is integrated with respect to a reference voltage. The output voltage of the integrator will increase if the peak detector output is higher than that of the reference voltage. The signal from the integrator is compared with a triangular waveform and the pulses produced are used to drive a power transistor which controls the ballast load. The time constant of the integrator is adjustable to enable the controller to be tuned to match the characteristics of the turbine-generator system.

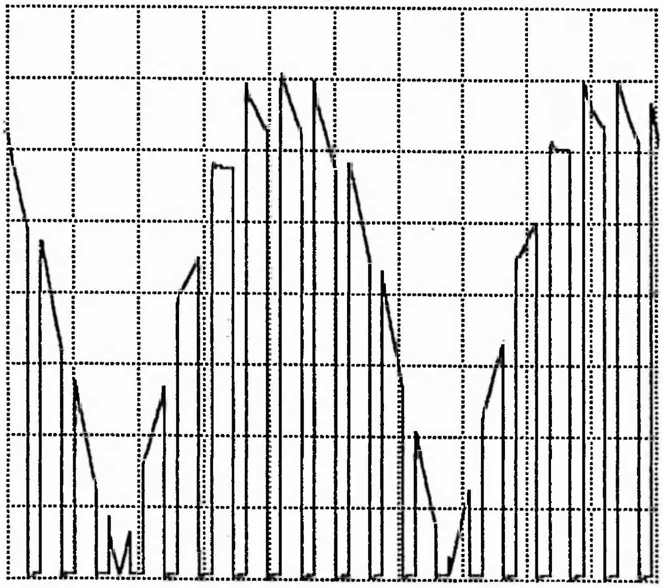
In order to extend the power range of the controller, circuitry was added to detect when the on-time of the ballast was 100% in order to switch in an additional ballast by means of a solid-state relay. The same circuitry was used to switch the additional ballast off when the on-time of the main ballast was 0%. Stability is maintained provided that the additional ballast is of a lower wattage than the main ballast. An 11 kW controller, further described in section 5.4.2, was built with a main ballast of 6.5 kW and an additional ballast of 4.5 kW. This avoided the expense and complication of fan-cooled heatsinks, which would have been required if a main ballast of 11 kW had been used.

5.3.3 Consumer load protection circuitry

Consumer load protection was designed into the mark-space ratio controller for the same reasons as those given for the binary-weighted controller. These reasons were listed in section 5.2.4. The same design approach was used for both overvoltage and undervoltage protection. The undervoltage trip was designed with an adjustable voltage setting, since the sensitivity required will vary according to the types of consumer load connected. For controllers of more than 1 kW capacity, instead of using the consumer load relay to switch



(a) Generator output voltage (100 V/div, 5 ms/div)



(b) Ballast load voltage (50 V/div, 2 ms/div)

Figure 5.4 Oscilloscope traces for a single phase IGC with approximately 65% on-time

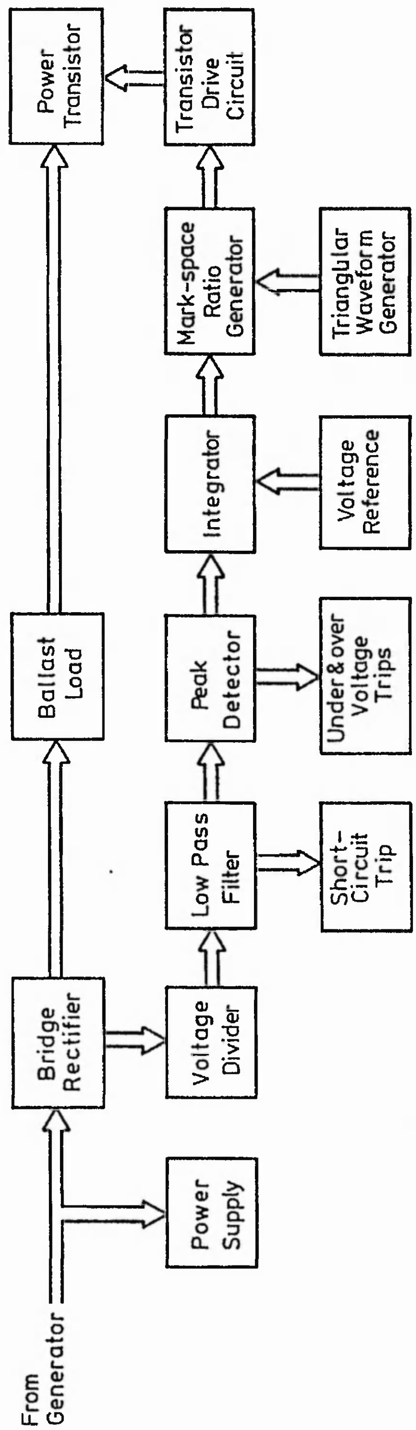


Figure 5.5 Block diagram of mark-space ratio IGC

the load directly, the relay connects the coil of a contactor that then switches in the load.

As a result of field experience, an additional trip for severe overload or short-circuit conditions was found to be necessary. Without this trip a short-circuit would cause the generator to de-excite. The loss of voltage would then cause the contacts of the consumer load contactor to open, thereby clearing the short-circuit. With no load now connected, the generator re-excites to the voltage at which the contactor coil becomes energised. The contactor contacts close into the short-circuit and the process repeats itself until the sustained undervoltage protection operates. The continual opening and closing of contacts under short circuit conditions is very damaging for the contactor. To overcome this problem a fast trip was added to the design which detects within 10 milli-seconds that the voltage has fallen to less than 25% of its rated value. The voltage sensing is taken from the input to the peak detector, as the slow discharge of the peak detector capacitor prevents a rapid response at the output. The complete design is explained in sections 4.20 and 4.21 of appendix K.

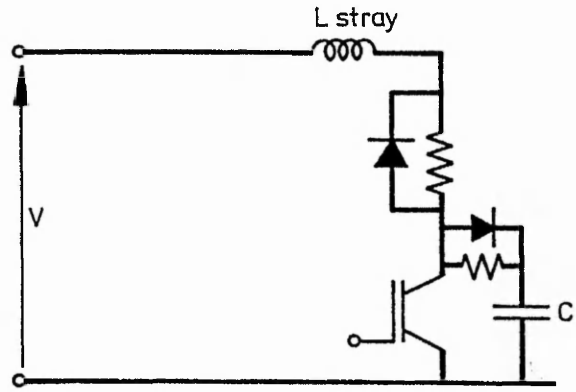
5.3.4 Protection of the power electronics

Protection of the power transistor

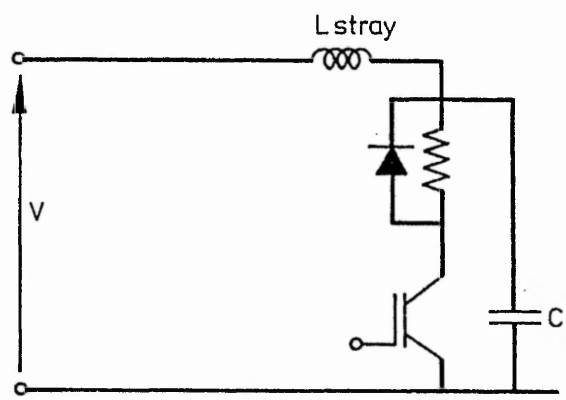
An insulated gate bipolar transistor (IGBT) was used in preference to a mosfet or bipolar transistor because the drive circuitry required is simple and cheap, they are available with high breakdown voltages and short-circuit protection is straight forward. The IGBT must be protected in case a short-circuit across the ballast occurs. IGBTs are available with short-circuit withstand times of 10 micro-seconds, which is sufficient time to detect the fault, by means of a sense resistor, and switch off the gate of the IGBT. The protection circuit triggers a thyristor, which clamps the IGBT in the off-state, thereby preventing damage to the IGBT through repetitive swiching into a short-circuit. The complete design is explained in section 4.14 of appendix K.

The ballast load will be largely resistive, but will have an inductive component particularly if it is a coiled space or water heater. In order to protect the IGBT from voltage spikes due to switching off this load a freewheeling diode was used. To protect against inductance in the transmission line or wiring from the generator to the controller a capacitor is placed across the inputs to the bridge rectifier.

Stray inductance exists on the output side of the rectifier and for this reason a snubber circuit was used. The snubber capacitor must be large, to protect the IGBT when it is switched out in the event of a short circuit, and therefore there is significant power



(a)



(b)

Figure 5.6 (a) Standard snubber circuit (b) Improved snubber circuit dissipated in the discharge resistor if the standard snubber circuit of figure 5.6(a) is used. To overcome the need for this resistor, the snubber circuit was modified by using the ballast to act as the discharge resistor for the snubber capacitor, as shown in figure 5.6(b).

Protection of the solid-state relay and bridge rectifier

Both of these devices can be protected against overcurrents by semiconductor fuses. The overvoltage protection of the IGBT also protects the bridge rectifier. Solid state relays are commonly available with inbuilt snubber circuits.

Lightning protection

Whilst it is virtually impossible to protect the IGC against a direct lightning strike, these are rare. Indirect strikes are much more common and can be easily protected against. Tests were performed with typical lightning waveshapes of up to 6 kV open circuit voltage and 3 kA short circuit current. The rated maximum voltage for the power electronics in the IGC is 1 kV.

The excitation capacitors themselves can be used to help protect the IGC. The typical energy to be absorbed to clamp a 6 kV (open circuit) pulse to 1 kV is 80 Joules [81]. This requires 160 uF of capacitance and is therefore impractical for small generating systems. Metal oxide varistors are required for small IGCs, fitted as shown in figure 5.7.

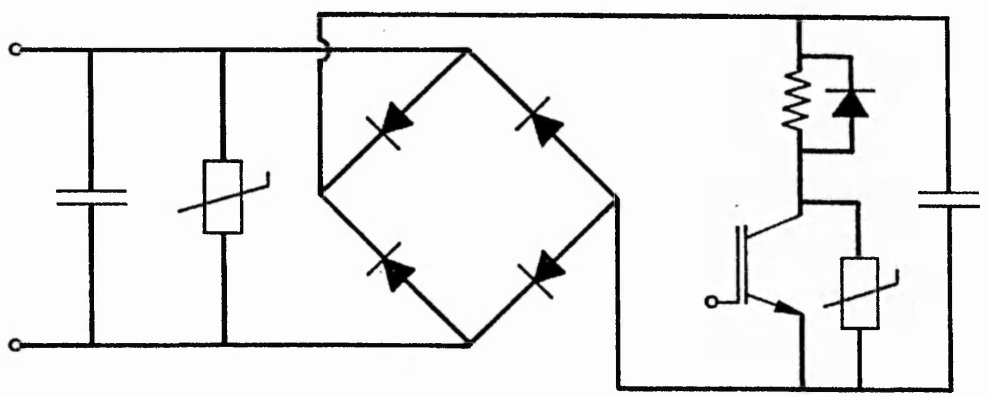


Figure 5.7 Lightning protection for the IGC

In tests the IGBTs were found to be more sensitive to voltage spikes and for this reason a varistor was connected across the IGBT as well as the bridge rectifier. The ballast load limits the current into this varistor and results in the voltage being clamped at a lower level. For controllers with an additional ballast fitted a varistor should be placed across the output of the solid-state relay.

5.3.5 Detailed controller design

See appendix K.

5.3.6. Three phase mark-space ratio IGC

The same principal used for the single phase mark-space ratio IGC can be used to produce a three phase version, as shown in figure 5.8. The same printed circuit board can be used with just a few changes to component values. The main change is to the potential divider ratio for the voltage sensing circuit. The ballasts must be rated for the full rectified voltage. This can be achieved by making the ballast from a series connection of two or three identical heater elements, rated for the phase voltage.

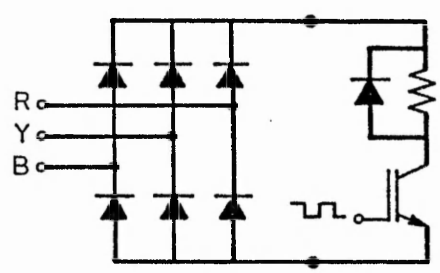


Figure 5.8 Basic switching circuit for the three phase mark-space ratio IGC

Whilst this controller does not compensate for any consumer load inbalance and produces additional waveform distortion, it is a very cheap and simple design. For relatively small machines, the extra cost involved in over-rating the generator to compensate for load inbalance and waveform distortion is more than compensated for by the saving in the cost and complexity of the controller. A three-phase electronic load controller with phase balancing is typically twice the price of a single-phase controller of the same capacity [82], whereas a three phase IGC is approximately the same cost as a single-phase IGC of the same capacity [83]. The use of the same circuit board and design approach as for the single-phase controller is an additional advantage, particularly for local manufacture in developing countries.

5.4 FIELD TESTING AND EVALUATION

5.4.1 Field testing of the binary-weighted controller

UK field testing

The controller was tested at the National Centre for Alternative Technology in Wales, from November 1987 to April 1988. A 2.2kW 4-pole single-phase induction machine was used, belt driven by a pelton turbine. Because of algae growth in the penstock pipe the electrical power output was just 650W. With no consumer load connected nine steps of ballast were switched in by the controller. The voltage was controlled between 195 and 225 Volts. The generator and controller both performed reliably and supplied mostly fluorescent and incandescent lighting.

Overseas field testing

In May 1988, the controller that had been tested in Wales was installed at Dharke, a small village near Kathmandu, Nepal. A three phase 4-pole induction machine was driven by a locally made pelton turbine. Just one phase of the generator was loaded and the maximum power supplied was 600W. The ballast loads were used to heat water. The main loads included lights, radios and even a video recorder.

The controller worked reliably. However, one drawback of the system was that during the dry months the flow-rate available for the turbine was less than required and so the turbine ran at reduced output. This reduced the number of steps of ballast and as a result the voltage window had to be increased in order to prevent instability. For this reason, in March 1990 the controller was replaced with a mark-space ratio controller.

5.4.2 Field testing of the mark-space ratio controller

UK field testing

Three mark-space ratio IGCs were installed in the UK; at Batworthy farm in Devon, Caudwell's mill in Derbyshire and Tennant Gill farm in Yorkshire.

Batworthy farm

A micro-hydro scheme had existed here since 1948. A Turgo turbine on 19 metres head was being used to directly drive a 240 V, 11 kW d.c 475 r.p.m. generator. To enable a standard induction machine to be employed a belt drive was used to increase the normal operating speed of the generator to 1550 r.p.m.. A 4-pole 15 kW three-phase 240 V delta connected induction machine was used as a single-phase generator by means of the "C-2C" capacitor connection, explained in section 2.1.5. A ballast load of 11 kW was used, divided between a 6.5 kW main ballast and a 4.5 kW additional ballast.

The generator and controller were installed in October 1990 and have been in continuous operation. The main loads are heating and lighting. The system has worked very reliably and the owners of the farm are pleased with the improved voltage regulation and provision of ballast load heaters, which they use to keep their saddle room dry. The only disadvantage of the installation has been some radio interference on medium wave and long wave. This mainly originates from the leads going to the main ballast loads, despite an earthed metal sheath being used.

Caudwell's mill

The turbine used at Caudwell's mill is an open-flume Francis turbine, dating back to the turn of the century and operating on approximately 2m head with a rated output of 50 h.p. at about 60 rpm. A 6-pole induction motor and belt-drives had been installed so that, in the case of a water shortage or problem with the hydro system, this could be used to power the milling equipment. This induction motor was replaced by a 15 kW machine, wound for 240 Volts per phase and star-connected. The milling load was removed and the motor used as a generator, driven by the turbine through the gearing that was already present. A 12 kW three-phase IGC was installed.

The generator and controller were installed in October 1990 and are operated for six to ten hours per day, five to seven days per week. The main loads are heating and lighting, though, a small motor and a video recorder are also used. In order to prevent damage to the power electronics due to overvoltages at runaway, a contactor was fitted that would disconnect the power electronics in the event of an overvoltage. However, this contactor was found to cause more damage than it prevented as the chattering of its contacts, when closing at power up, produced voltage spikes, due to charging and discharging of stray capacitance, that damaged the power electronics in the controller. For this reason it was removed and the correct functioning of the ballast loads is now relied upon to prevent overvoltages from occuring.

Tennant Gill farm

This was a completely new scheme. Three close-coupled pump-motor units were installed as turbine-generators on a site with a head of 65 metres [84]. Three turbines, each requiring a different flow-rate, were used because with reverse pumps the flow cannot be adjusted. The outputs of the three units were 800 W, 2000 W and 4000 W. In each case three-phase, 2-pole induction machines were used and connected C-2C for single-phase loading. A 4 kW IGC was installed and the ballasts were used to heat the farm house.

The installation was commissioned in February 1991. The main loads are lights, some of which are powered via a 24V battery charging system, and domestic appliances including: a vacuum cleaner, fridge, freezer, washing machine, radio, T.V., video recorder, iron and electric blankets. When first installed there was a problem in the controller due to damp reducing the electrical insulation between the IGBT and heatsink. This necessitated replacing the TO-3 style IGBT with an isolated module.

As with the Batworthy farm installation, radio interference on medium and long wave was caused by the switching of the ballast loads. Interference in the farm house can be avoided by siting the ballast loads in the turbine house, though the beneficial background heating is then lost. The radio interference was considered to be less of a problem once it was discovered that the Archers is also broadcast on FM!

Overseas field testing

In March 1990 the binary-weighted controller installed at Dharke in Nepal, described in section 5.4.1, was replaced by a mark-space ratio controller. Voltage regulation and response speed were very significantly improved and the controller settings no longer required adjusting for variations in turbine power output.

At installation, there was a problem with false triggering of the ballast load short-circuit protection. This was due to noise and was overcome by greater use of twisted pairs and routing the high current carrying wires away from the printed circuit board. This problem was later solved by redesigning the current sensing circuit so that it used a differential input configuration on the operational amplifier so as to produce a high common-mode rejection ratio.

Since March 1990 eight more mark-space ratio IGCs have been installed in Nepal, four in Sri Lanka and two in Indonesia. All have been built by local engineers who have been trained either at Nottingham Polytechnic or in their own country.

5.4.3 Evaluation of the two controllers

The conclusion as to which of the two controller designs is best can be drawn by considering their uptake in Nepal. Two locally made binary-weighted controllers were installed but once the mark-space ratio controller was introduced all subsequent installations have used this latter type of controller.

The main advantages of the mark-space ratio controller are as follows:-

1) Simpler ballast arrangement

2) Better voltage regulation

3) No adjustment required for reduced turbine power output

4) Faster response to load changes

The binary-weighted controller has the advantages of not producing any RFI and not causing waveform distortion. In the case of the mark-space ratio controller, RFI is negligible if the ballast load and wiring are away from the consumer loads, as is generally the case because the controller is usually installed in the turbine house.

5.5 CONCLUSIONS

Two types of induction generator controller have been developed, each using the constant voltage controlled frequency method. Both have been proven to work, both in the laboratory and in the field. The mark-space ratio controller has proven to be the most popular design and there are now eighteen units installed in the UK, Nepal, Sri Lanka and Indonesia.

CHAPTER 6

CONCLUSIONS AND FURTHER WORK

6.1 CONCLUSIONS

A detailed appraisal of the currently available generating systems for stand-alone micro-hydro schemes was undertaken, as presented in chapters one and two. Two main conclusions were drawn from this:-

(i) Induction generators are preferable to synchronous generators because they are more reliable and, for sizes below 20 kW, significantly cheaper.

(ii) The present voltage and frequency controllers for induction generators are more costly and complicated than those for synchronous generators. As a result, the full induction generator systems are no cheaper or simpler than the present, well proven, synchronous generator systems.

It was clear that a new control approach was required if the cost and complexity of induction generator systems was to be reduced to a level that would make them advantageous for micro hydro systems. The new and quite radical approach of this thesis was to allow controlled variations of the supply voltage and/or frequency in order to simplify the control system.

In order to achieve this, a fundamental investigation was carried out to determine the acceptable voltage and frequency regulation for small rural electrification schemes. It was concluded that a relaxation in standards can be made in comparison to the regulations for large grid systems, particularly with regard to frequency regulation. It was shown that in all cases the frequency can be allowed to increase significantly above its rated value. The extent of the increase being dependant upon the types of load anticipated.

All three possible alternatives for new control approaches were considered; controlled voltage and frequency (CVF), constant voltage and controlled frequency (CVCF) and constant frequency and controlled voltage (CFCV). A computer model, incorporating turbine and generator characteristics was developed in order to test the different control options. The accuracy of the model was proven by comparison with test results.

The main conclusions resulting from investigation of the three control approaches were as follows:-

CVF approach

This method has the advantage of requiring no electronic control equipment. However, regulation is very poor unless the turbine is operated close to its runaway speed and therefore at substantially reduced efficiency. It is not practical for most micro-hydro schemes.

CFCV approach

Excellent frequency regulation is achieved by the load controller. Good voltage regulation with variable power factor loads can be achieved provided that suitable values of series capacitance are used. The series capacitors add significantly to the cost of the generating system. The other disadvantage with this approach is the danger of sub-synchronous resonance occuring when starting induction motors.

CVCF approach

Excellent voltage regulation is achieved by means of the electronic controller and good frequency regulation can be obtained with variations in total load power factor down to 0.8 lagging. Improved frequency regulation can be obtained by power factor correcting significant inductive loads. This control approach is cheaper than the CFCV method and no problems with sub-synchronous resonance have been found.

The CVCF approach was considered to be the most suitable of the three approaches, and when compared to the existing control systems for induction generators has the advantage of fixed excitation, rather than the expense and complexity of a variable leading VAR source. A single ballast load controller is all that is required for the CVCF approach, compared to the ballast load controller and variable leading VAR source required for the conventional approaches. The cost and complexity of the control system is more than halved, since a variable leading VAR source is more expensive and involved than a load controller. An additional advantage of the CVCF approach is that the generator is inherently overload safe, since the voltage will collapse under overload conditions because of insufficient leading VARs.

The controller required for the CVCF approach is similar to the load controllers used with synchronous generators, the only difference being that the CVCF controller regulates voltage whereas load controllers regulates frequency. The cost of the two types of controller is similar. In fact the CVCF controller may be cheaper since voltage is easier to measure than frequency. The CVCF approach for induction generators results in a control system that is at least as cheap as controllers for synchronous generators and as a result makes induction generator systems a very attractive option for stand-alone micro-hydro power.

In order to prove that the CVCF control approach was suitable for stand-alone induction generators, two different controllers of this type (subsequently named Induction Generator Controllers (IGC)) were designed, built and tested. To date, all ballast load controllers use a phase-angle or binary weighted control approach, and indeed the initial IGCs were built with binary weighted ballasts. A variable mark-space ratio approach was found to offer advantages over both these types of controller because, in its simplest form, it requires just a single ballast load and provides a varying load of unity displacement factor. The variable mark-space ratio approach works well and has proven to be the prefered approach.

An important part of the development of this controller was a careful consideration of the environment in which the controller would be used and the support service available. For this reason it was very necessary to closely liase with field engineers in developing countries and design the controller as simply and robustly as possible, with inbuilt protection circuits to prevent damage to the controller or consumers in the event of all forseeable fault conditions.

The reliability of the IGC has been proven by installing three units on realistic field test sites in England, where they have been carefully monitored for nearly two years. The full success of the IGC is clear from the fact that, within less than two years, fifteen locally manufactured controllers have been installed on a commercial basis in Nepal, Sri Lanka and Indonesia and in each country the level of demand is increasing. This success is not simply a result of the IGC but the fact that the technology has been made available to local engineers in developing countries through training courses, enabling them to build cheap, reliable systems that they can maintain and repair.

6.2 FURTHER WORK

6.2.1 Introduction

The most important area of further work is the training of engineers in developing countries in the manufacture and installation of IGCs. This represents a natural progression of the work that has been started in Nepal, Sri Lanka and Indonesia. In addtition there is scope for research and development in the areas of induction generators and controllers.

6.2.2 Purpose-wound induction generators

To date, only factory wound induction motors have been used as generators. However, there are a number of advantages to be gained by using induction machines with stators specially wound for single-phase or three-phase generation. The increased cost is quite small, especially in developing countries where labour costs are low. The main advantages of purpose wound machines are as follows:-

Improved efficiency

Even if a larger frame size is used to obtain the higher efficiency, the small additional cost is more than compensated for by the increased output and revenue generation [85].

Reduced transmission costs

Since the machine is purpose wound, a high generating voltage can be used in order to reduce transmission costs, by avoiding the need for a step up transformer.

Flexible manufacturing

A standard range of frame sizes can be stocked, which is more economic than storing a full range of machines. These frames can then be wound for the speed, output power, output voltage and number of phases required.

Research on purpose winding, especially for single-phase generation, is being carried out at Nottingham Polytechnic [85].

6.2.3 Self-regulating induction generators

A further area of research and development on induction generators is the design of a self-regulating induction generator, to eliminate the need for an IGC. A degree of self-regulation already exists since at reduced load the turbine speeds up, increasing generated frequency and voltage and therefore the power dissipation within the machine. At no load the full power output of the turbine is dissipated in the generator. This results in too high a voltage and machine temperature and is not acceptable.

For self-regulation to be practical, the machine must be made with additional windings connected to a ballast load and the design optimised so that under normal voltage conditions little current is drawn by the ballast but as the voltage increases the ballast current increases rapidly, dissipating most of the surplus power away from the machine. A detailed investigation would be required to determine the regulation and efficiency of such a system. It is likely to be most appropriate for very small systems, where high efficiency and good regulation are not very important, but the savings, in terms of cost and complexity, resulting from a simple system are considerable.

6.2.4 New designs of IGC

New designs of IGC may be appropriate for both large micro-hydros, of 30 kW and above, and small micro-hydros, of below 1 kW. Initially it was thought that 30 kW was likely to be an upper limit for stand-alone micro-hydro schemes because above this size there is little or no cost advantage compared to using synchronous generators. However, the robustness and good availability of induction machines and more importantly the availability of a cheap, reliable controller that can be locally manufactured has resulted in enquiries for larger units, up to at least 50 kW.

The larger units will invariably be three-phase. IGBTs and bridge rectifiers of sufficient capacity are available for controllers up to and above 100 kW, though the disadvantages of no phase balancing and waveform distortion become more significant at this size. In addition, the advantages of a single switching element and single ballast diminish because of the difficulties of switching such high currents and the fact that the single ballast is made of many water heaters in parallel. A phase balancing binary-weighted controller becomes a better option for these sizes. Careful design will be required, in terms of fault-tolerance, in order to prevent the failure of a single ballast element from causing instability.

There is a growing interest in very small micro-hydros, of less than 1 kW, especially for battery charging [86,87]. Schemes have been installed using vehicle alternators, though they tend to be unreliable, as they are not designed for continuous operation. Also, standard a.c. loads cannot be connected and the battery charging must take place in the turbine house because of the high transmission line losses associated with low voltage systems. By using induction generators and IGCs these disadvantages can be overcome, though a high powered battery charger would be required. It should be relatively straight forward to redesign the IGC to operate as a battery charger and generator controller, saving cost and complexity. As well as accurately controlling the battery charging, the controller will produce a regulated a.c. supply that is acceptable for most purposes.

ACKNOWLEDGEMENTS

I would like to acknowledge the following people for their help during the project.

Ray Holland of ITDG and Dr Peter Holmes for initiating the project.

ITDG, SERC, ODA and Nottingham Polytechnic, for funding.

The staff at ITDG, especially: Andy Brown, Adam Harvey and Mark Waltham, for their support and for keeping me sane by sending me overseas.

Dr Keith Pratt, Arthur Williams and Peter Bell, for getting involved in the 'nitty-gritty', giving honest opinions and for their encouragement.

The technicians in the electrical and mechanical engineering departments, especially Jeff Baines, George Stuart, Bob Oreszczyn, Ralph James and Alan Crisp, for their assistance with lab tests and site installations.

Dr Brian O'Neill for his help particularly regarding production of the thesis.

Fellow researchers in the department, for their advice and support.

Fiona Williams in the library, for her encouragement to use their excellent facilities.

Dr Bob Todd and the staff at the National Centre for Alternative Technology, for the provision of site-test facilities.

Bill and Anne Cowperthwaite at Tennant Gill farm, for their hospitality, humour and large measures of whisky.

Ben Van Wijhe and Bikas Pandy, for teaching me about micro-hydro in Nepal and providing advice on the application of stand-alone induction generators.

Akkal-Man Nakarmi and Surrendra Mathema, at Kathmandu Metal Industries, for their enthusiasm and willingness to try out the induction generator controller and for useful discussions on possible improvements.

My family and friends for their tolerance and support.

REFERENCES

1. UNITED NATIONS. "Report of the technical panel on hydropower" U.N. Conference on New and Renewable Sources of Energy, 1980.

2. HANGZHOU REGIONAL CENTRE FOR SMALL HYDRO POWER. "Small hydro power in China" (Intermediate Technology Development Group, Rugby, 1985).

3. BINGLI, D. "Small hydro in China: progress and prospects" Water Power and Dam Construction, 1985, 37, (2), pp.15-7.

4. DAFFERN, E.S. "World Bank policy in the promotion of energy producing projects in developing countries" 5th IEE Energy Options conference, 1987.

5. HOLLAND, R.E.A. "Micro-hydro electric power" (Intermediate Technology Development Group, Rugby, 1983) p.3.

6. DEPARTMENT OF ENERGY. "Renewable Energy in the UK: The way forward" Energy paper no. 55, 1988.

7. FROST AND SULLIVAN. "The Worldwide market for remote site power systems" (Frost & Sullivan, 106 Fulton Street, New York, N.Y. 10038).

8. SMITH AND CO-AUTHORS. "Report of the Regional Rural Electrification Survey to the Asian Development Bank." US Agency for international development.

9. HOLLAND, R.E.A., ARMSTRONG-EVANS, R.J., MARSHALL, K. "Community load determination, survey and system planning" Small Hydroelectric powerplants, NRECA, Washington, D.C., 1980.

10. WILLIAMS, A.A. "The potential for using induction motor-pump units as micro-hydroelectric generators in Nepal" Trent Polytechnic, Nottingham, 1988, pp.3-4.

11. TWIDELL, M. "Power to the outback" IEE Review, July/August 1988, pp.268-9.

12. FRITZ, J.J. "Small and mini hydropower systems" (McGraw-Hill, 1984) p.11.7.

13. DAUGHERTY, R.L., FRANZINI, J.B., FINNEMORE, E.J. "Fluid mechanics with engineering applications" (McGraw-Hill, 1985, 8th edition) pp.477-8.

14. INVERSIN, A.R. "Micro-hydropower sourcebook" (NRECA, Washington, D.C., 1986) p.172.

15. DOUGLAS, J.F., GASIOREK, J.M., SWAFFIELD, J.A. "Fluid mechanics" (Pitman, 1985, 2nd edition) pp.613-5.

16. KITTREDGE, C.P. (Section Author) "Pump Handbook" (McGraw-Hill, 1986, 2nd edition) p.2.251.

17. HOLLAND, R.E.A. "Machinery and equipment for micro hydro plants" Water Power and Dam Construction, 1986, 38, (11), pp.17-9.

18. RIDLEY, G.K. "The case for refurbishing and uprating hydro-generators" GEC Review, 1988, 4, (2), pp.83-93.

19. JOHNSON, F.G. "Hydro-electric power in the UK: past performance and potential for future development" IEE Proc. C, 1986, 133, (3), pp.110-120.

20. RIJAL, K. "Evaluation of small hydel units in Nepal" Energy, 1986, 11, (3), pp.265-9.

21. APPROPRIATE TECHNOLOGY DEVELOPMENT ORGANIZATION. "Mini and micro hydel plants" A report (1-B 47th street, F-7/1, P.O. Box 1306, Islamabad, Pakistan).

22. RESEARCH CENTRE FOR APPLIED SCIENCE AND TECHNOLOGY. "Renewable Energy Resouces in Nepal" Proc. Workshop Seminar, Tribhuvan University, Kathmandu, 1984.

23. HOLLAND, R.E.A. "The role of micro hydro in developing countries". Water Power and Dam Construction Supplement, 1985, 37, (11), pp.2-3.

24. NAKARMI, A.-M., BACHMANN, A. "MPPU Multi-Purpose Power-Unit" (Sahayogi Press, Kathmandu, 1983, 2nd edition).

25. WILLIAMS, A.A., SMITH, N.P.A., MATHEMA, S. "Application of appropriate technology to small-scale hydroelectric power" Science Technology and Development, 1989, 7, (2) (Cass) pp. 98-112.

26. ENGEDA, A., RAUTENBERG, M. "Performance of centrifugal pumps as hydraulic turbines" 4th international hydropower machinery symposium, ASME, 1986.

27. BASSET, E., POTTER, F. "Capacitive excitation for induction generators" Trans AIEE, 1935, 54, pp.540-5.

28. WATSON, D.B., "Circuit model and self-excitation of the induction generator" Int. J. Elect. Eng. Educ., 1988, 25, (2), pp. 163-70.

29. ELDER, J.M., BOYS, J.T., WOODWARD, J.L. "The process of self-excitation in induction generators" IEE Proc. B, 1983, 130, (2), pp. 103-8.

30. MURTHY, S.S., MALIK, O.P., TANDON, A.K. "Analysis of self-excited induction generators" IEE Proc C, 1982, 129, (6), pp. 260-5.

31. MALIK, N.H., HAQUE, S.E. "Steady state analysis and performance of an isolated self-excited induction generator" IEEE Trans. Energy Conversion, 1986, EC-1, (3), pp. 134-9.

32. SMITH, N.P.A., WILLIAMS, A.A., HARVEY, A.B., WALTHAM, M. NAKARMI, A.M. "Directly coupled turbine-induction generator systems for low-cost micro-hydro power" Proc. Second World Renewable Energy Congress, 1992 (Pergammon Press).

33. MURTHY, S.S., SINGH, B.P., NAGAMANI, C., SATYANARAYANA, K.V.V. "Studies on the use of conventional induction motors as self-excited induction generators" IEEE Trans. Energy Conversion, 1988, EC-3, (4), pp. 842-7.

34. SHUMOV, Y. N. "Single phase asynchronous generator" Soviet electrical engineering USA, 1976, 47, (6), pp. 44-5.

35. BHATTACHARYA, J. L., WOODWARD, J.L. "Excitation balancing of a self-excited induction generator for maximum output" IEE Proc. C, 1988, 135, (2), pp. 88-97.

36. HOLLAND, R.E.A. "Experience with electronic load controllers for micro hydro plants" GTZ conference on simple regulating systems for micro-hydro-power plants, Frankfurt 1988.

37. SCHWEPPE, F.C., TABORS, R.D., KIRTLEY, J.L., OUTHRED,H.R., PICKEL, F.H., COX, A.J. "Homeostatic utility control" IEEE Trans. Power Apparatus and Systems, 1980, PAS-99, (3), pp. 1151-63.

38. MEIER, U. "Local experience with micro-hydro technology" (SKAT Publications, St Gallen, Switzerland, 1983).

39. MEIER, U. "The Dhading micro-hydropower plant", Working paper WP 6/83 (SKAT Publications, St Gallen, Switzerland, 1983).

40. HENDERSON, D.S., MACPHERSON, D.E. "Electronic load governing for micro-hydro generating units" 22nd UPEC conference 1988.

41. PITTET, A. "Load controllers for micro hydel plants", Proc. National Solar Energy Convention, 1981, pp. 11.005-11.007.

42. WATSON, D.B., WATSON, R.M. "Microprocessor control of a self-excited induction generator" Int. J. Elect. Eng. Educ., 1985, 22, (1), pp. 69-82.

43. REGIONAL (ASIAN & PACIFIC) RESEARCH AND TRAINING CENTRE FOR SMALL HYDROPOWER HRC, HANGZHOU, CHINA "Report on the testing and trial operation of an electronic load controller" 1983.

44. KORMILO, S., ROBINSON, P. "Electronic control of small hydroelectric schemes using a microcomputer" IEE Proc. E, 1984, 131, (4), pp. 132-6.

45. ELDER, J.M., BOYS, J.T., WOODWARD, J.L. "Integral cycle control of stand-alone generators. IEE Proc. C, 1985, 132, (2), pp. 57-66.

46. SPITTAL, W. "A practical microhydro using standard pumping equipment" Trans. NZIE, 1983, pp. 56-64.

47. FISCHER, G. "Design of a simple oil-hydraulic controller for micro-hydro-power plants" GTZ conference on simple regulating systems for micro-hydro-power plants, Frankfurt, 1988.

48. ROBINSON, P. "Microhydro in Papua New Guinea - the experiences of an electrical engineer" IEE Power engineering journal, September 1988, pp. 273-80.

49. EL-SHARKAWI, M.A., VENKATA, S.S., WILLIAMS, T.J., BUTLER, N.G. "An adaptive power factor controller for three-phase induction generators" IEEE Trans. Power apparatus and systems, 1985, PAS-104, (7), pp. 1825-31.

50. ELDER, J.M., BOYS, J.T., WOODWARD, J.L. "Self-excited induction machine as a small low-cost generator" IEE Proc. C, 1984, 131, (2), pp.33-41.

51. BERNAYS, D.J. "A low-cost single-phase induction generator" IEEE Power electronics specialists conference, 1982, pp. 185-96.

52. BRENNEN, M.B., ABBONDANTI, A. "Static exciters for three-phase induction generators" IEEE Trans. Industrial applications, 1977, IA-13, (5), pp. 422-8.

53. HAMMAD, A.E., MATHUR, R.M. "A new generalised concept for the design of thyristor phase controlled VAR compensators" IEEE Trans. Power apparatus and systems, 1979, PAS-98, (1), pp. 219-31.

54. DORADLA, S.R., PATEL, B.K. " A thyristor reactive power factor compensator for fast varying industrial loads" Int. J. of Electronics, 1981, 51, (6), pp. 763-77.

55. IRIBARNEGARY, L.S., AMBROSIA, S.B., VICENTE, A.C., GUASP, M.R. "Static VAR compensator with asynchronous generator with or without connection to the network" Proc. First European conference on power electronics and applications, 1985, pp.4.11-18.

56. IRISA, T., TAKATA, S., UEDA, R. "Dynamic performance and voltage stability of self-excited induction generator with voltage controller" Trans. Inst. Elec. Eng. Japan, section E, 1982, 102, (1/2), pp.1-8.

57. MALIK, S.S., DIVAN, D., MURTHY, S.S., GRANT, T., WALSH, P. "A solid state voltage regulator for self-excited induction generators" IEEE Industrial and commercial power systems conference, 1983, pp. 139-43.

58. BOYS, J.T., ELDER, J.M., FORSTER, M.K., WOODWARD, J.L. "A low-cost AC generating system suitable for use with small hydro plants" NZIE Transactions, 1981, 8, (3), pp. 75-84.

59. EPSTEIN, E., YAIR, A., ALEXANDROVITZ, A. "Analysis of a reactive current source to improve the current drawn by static inverters" IEEE Trans. Industrial electronics and control instrumentation, 1979, IECI-26, (3), pp. 172-7.

60. BARRETO, A.N., WATANABE, E.H. "Self-excited induction generator/force-commutated rectifier system operating as a DC power supply" Proc. First European conference on power electronics and applications, 1985, pp.3.185-189.

61. WOODWARD, J.L. "Village microhydro - Can it be appropriate?" First international conference on technology for development, 1980, pp. 275-280.

62. GIDDENS, E.P. "A direction for research in microhydropower" Third international symposium on wave, tidal, OTEC and small scale hydro energy, 1986, pp. 43-52.

63. THORN EMI LIGHTING CATALOGUE, 1984/5 p. 246.

64. HINDMARSH, J. "Electrical machines and their applications" (Pergamon Press, 1977, 3rd edition) p.385.

65. HOWATSON, A.M., LUND, P.G., TODD, J.D. "Engineering tables and data" (Chapman and Hall, 1972), p. 75.

66. APEDAILE, L.P. "Hydraulic governors for rural mini hydro-electric developments" Appropriate technology, 1988, 15, (1), pp. 14-16.

67. SMITH, N.P.A. "A low-cost electronic load controller for water-powered induction generators" GTZ conference on simple regulating systems for micro-hydro-power plants, Frankfurt 1988.

68. ABDEL-KADER, F.E., HOLMES, P.G. "Induction generator compensation by series capacitor tuning" Proc. UPEC, 1983, pp. 713-18.

69. SMITH, N.P.A. "The stabilisation of a self-excited induction generator for small hydro-power applications" Proc. UPEC, 1986, pp.261-4.

70. GRANTHAM, C. "The determination of induction motor parameters and their application in the steady-state and transient analysis of dynamic braking systems" Ph.D. Thesis, 1972, University of Newcastle-Upon-Tyne.

71. WARE, D.H. "Measurement of stray-load loss in induction motors" Trans. AIEE, 1945, 64, pp. 194-7.

72. HAMELS, D. "Application of different equivalent circuits in determining induction machine performance in practical situations" Proc. First International Conference on Electrical Machines, 1984, pp. 380-3.

73. ALGER, P.L. "Induction machines" (Gordon and Breach Science Publishers, 1970, 2nd edition) p.122-3.

74. BAKER, R.J., NELDER, J.A. "The GLIM system manual" (Numerical Algorithms Group, 1978, release 3).

75. BUTLER, J.W., CONCORDIA, C. "Analysis of series capacitor application problems" Trans. AIEE, 1937, pp. 975-88.

76. WAGNER, C.F. "Self-excitation of induction motors with series capacitance" Trans AIEE, 1941, 60, pp. 1241-7.

77. MAHMOUD, A.A., ORTMEYER, T.H., HARLEY, R.G. "Effects of reactive compensation on induction motor dynamic performance" IEEE Trans. Power apparatus and systems, 1980, PAS-99, (3), pp. 841-6.

78. LIMEBEER, D.J.N., HARLEY, R.G. "Subsynchronous resonance of single-cage induction motors" IEE Proc. B, 1981, 128, (1), pp. 33-42.

79. DE OLIVEIRA, S.E.M. "Starting transients of saturated induction motors with series capacitance on the supply" IEEE Trans. Energy conversion, 1986, EC-1, (3), pp. 205-10.

80. VAN WIJHE, B. "The digital load controller a handbook" (Development and Consulting Services, Butwal, Nepal, 1989).

81. GENERAL ELECTRIC "Transient voltage suppression manual" (General Electric, 1983, 4th edition) p. 28.

82. G.P. ELECTRONICS, Bovey Tracey, Devon - Price list 1991.

83. SMITH, N.P.A. "Induction generator training course in Indonesia" - Report for the Intermediate technology development group (ITDG, Rugby) - 1992.

84. WILLIAMS, A.A. "Micro-hydroelectric demonstration scheme at Tennant Gill farm, Malham" - Report for the Intermediate technology development group (ITDG, Rugby) - 1991.

85. BELL, P.B. "Costs and benefits of dedicated induction generator designs" - Report for the Intermediate technology development group (ITDG, Rugby) - 1992.

86. FOLEY, G. "Electricity for rural people" (Panos, 1990).

87. Rodriguez, E. "Electrificacion rural basada en baterias de 12V" Hidrored, 1991, (2), pp. 2-4.

88. CHAPRA, S.C., CANALE, R.P. "Numerical methods for engineers" (McGraw-Hill, 1988, 2nd edition) p.218-20.

89. DRAPER, N.R., SMITH, H. "Applied regression analysis" (J.Wiley, 1981, 2nd edition) p.10-1.

90. BAKER, R.J, NELDER, J.A. "The GLIM system manual" (Numerical Algorithms Group, 1978, release 3).

91. SPIEGEL, M.R. "Theory and problems of statistics" (McGraw-Hill, 1972) p.243.

92. CHENG, W. "Reverse running of a centrifugal pump" - Final year project thesis, Nottingham Polytechnic - 1983.

APPENDIX A

CONSTANT VOLTAGE VARIABLE FREQUENCY OPERATION OF A CAGE INDUCTION MACHINE WITH FAN-TYPE LOADING

A synchronous generator driven by a variable speed d.c. motor was used to provide a variable frequency supply. This was used to drive the induction motor, which was loaded by a d.c. machine operated as a generator. A digital wattmeter was used to measure the electrical input, a torque transducer was used to determine the load and a stator mounted thermocouple provided temperature information. A block diagram of the experimental set up is given in figure A.1. At 50 Hz the motor was operated at rated voltage and full load, i.e. 2.2 kW as measured by the torque transducer. For the higher frequency conditions rated voltage was also used and the load increased by the factor $(F_{new}/50)^3$, where F_{new} was the new value of frequency. For each frequency condition the machine was allowed to warm up to a steady state before readings were taken. This was achieved by determining $T_{mot} - T_{amb}$, where T_{mot} was the thermocouple reading and T_{amb} the ambient temperature. Readings were taken every 10 minutes until the value of $T_{mot} - T_{amb}$ stabilised.

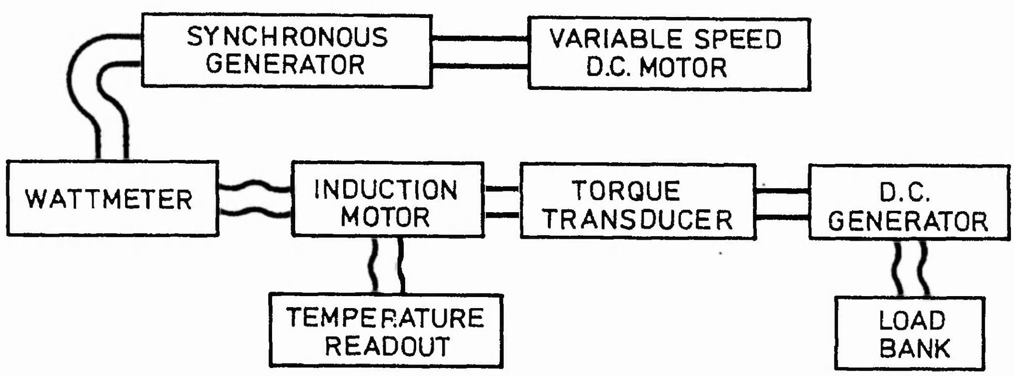


Figure A.1 Test arrangement for variable frequency operation and variable loading of an induction motor

APPENDIX B

INDUCTION MOTOR PARAMETER DETERMINATION TESTS

APPENDIX B1
Stator d.c. resistance and thermocouple calibration

Calibration of the thermocouples was achieved by performing d.c. resistance measurements; firstly with the windings at room temperature and then after operating the machine under no-load and full-load conditions. For the no-load and full-load tests the machine was run for two hours before measurements were taken, in order to allow a state of thermal equilibrium to be reached.

The d.c. resistance was obtained by standard voltmeter and ammeter measurements, and included compensation for the resistance of the measuring leads. Once the machine was switched off the hot resistance was measured as quickly as possible. After the initial measurement, repeat measurements were taken for three minutes as the machine cooled. The resistance changes, ΔR, relative to the final resistance value were calculated and the cooling curves of log ΔR against time were plotted, see figures B1.1. The curves were extrapolated back to the time at which the machine was switched off, in order to determine the operating resistance. The mean winding temperature was then calculated, using the standard formula given by equation B1.1. The results are displayed in table B1.1.

$$T_{new} = \left[\frac{R_{new}}{R_{old}} \times \left(234.5 + T_{old}\right) \right] - 234.5 \qquad \text{........... B1.1}$$

where T_{new} and T_{old} are in °C

T_1	T_2	R(Ω)	T_{amb}	T_{wind}	$(T_1+T_2)/2$
21.9	22.1	2.923	22.0	-	22.0
66.0	63.5	3.380	25.0	62.0	64.8
79.1	76.2	3.520	24.3	74.4	77.7

Table B1.1 Mean winding temperatures and thermocouple readings (°C)

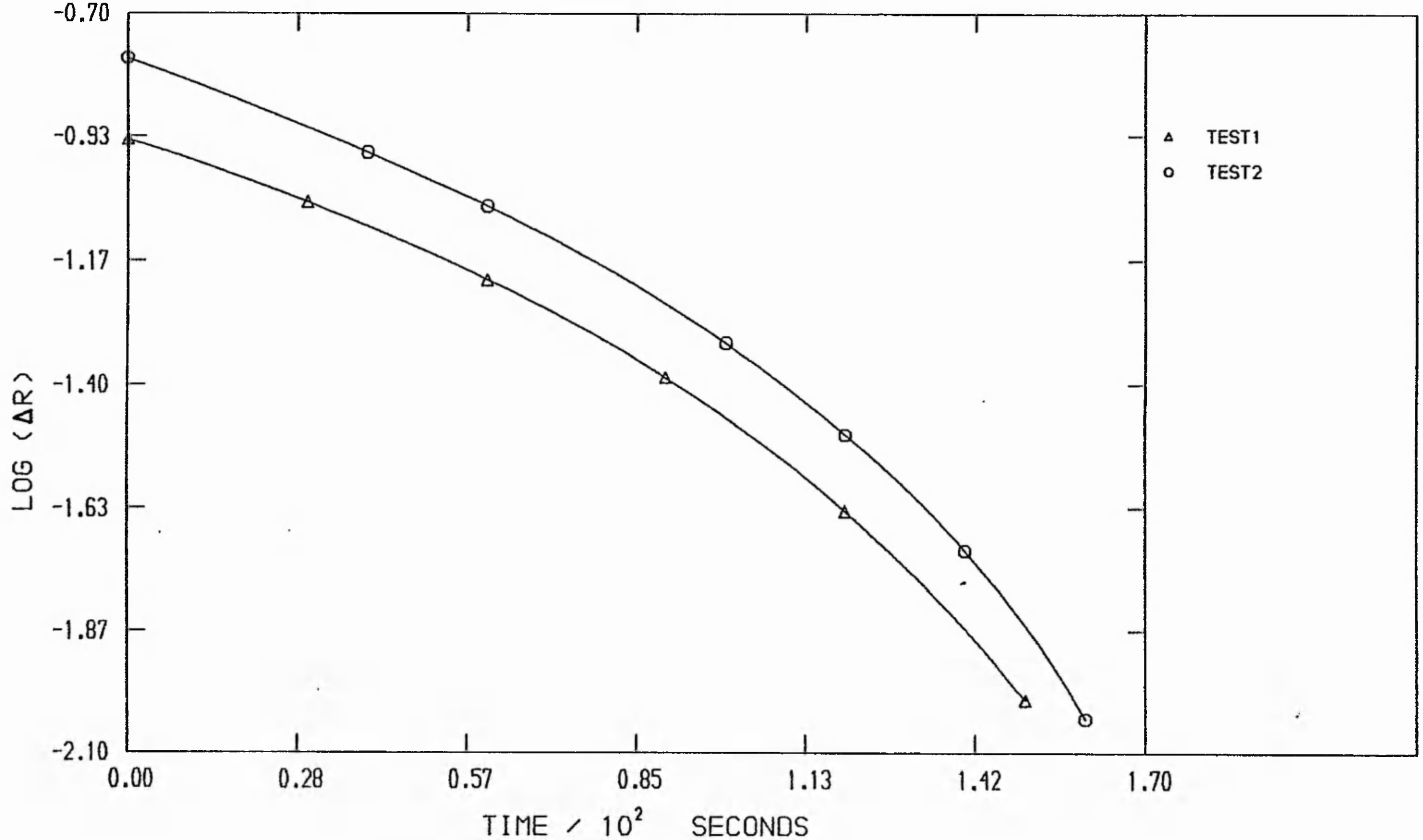


FIG. B1.1 Cooling Curves

The results displayed in table B1.1 indicate that the thermocouples readings are quite accurate, particularly near room temperature. At higher temperatures the thermocouples read too high, indicating that they are probably situated too close to the centre of the winding. The mean thermocouple reading was 2.75°C too high for the no-load test and 3.25°C too high for the full-load test. To account for this 3°C was subtracted from the thermocouple output for higher temperature readings.

APPENDIX B2
a.c. stator resistance test

In order to perform an a.c. resistance test the rotor of the machine was removed. The machine was star connected and fed, at near rated current, by a variac connected to the mains supply. Once the thermocouple readings had stabilised, the voltage and current per phase were measured using a digital wattmeter. The results for each phase are given in table B2.1. The mean resistance value was 3.56 Ω.

V (Volts)	I (Amps)	R (Ohms)
21.07	3.510	3.571
20.98	3.500	3.567
21.21	3.545	3.541

Table B2.1 a.c. resistance results

The d.c. resistance at the operating temperature was obtained by means of a cooling curve, similar to those of figure B1.1. The result, determined by extrapolation, was 3.482 Ω per phase.

The a.c. resistance result includes core-loss. This can be accounted for, though the circumstances are not completely identical, by doing a no-load test at the same flux level. To achieve this the voltage across the leakage inductance must be calculated using equation B2.1. In this case, the mean voltage, V_{leak}, was 17.0 V.

$$V^2_{leak} = V^2_{phase} - (IR)^2 \qquad \text{.......... B2.1}$$

where R is the calculated value of a.c. resistance

APPENDIX B3
Locked rotor test

The locked rotor test was carried out at rated current, after a period of full-load operation used to stabilise the temperature within the machine. The three phase wattmeter results are given in table B3.1.

$V_{average}$ (Volts)	$I_{average}$ (Amps)	W (Watts)	VA (Volt-Amps)	T1 (°C)	T2 (°C)
43.8	4.59	344	599	67.3	65.5

Table B3.1 Locked rotor results

APPENDIX B4
No-load tests with motor driven at synchronous speed

$V_{average}$ (Volts)	$I_{average}$ (Amps)	W (Watts)	T1 (°C)	T2 (°C)
258.73	5.003	348	75.0	72.5
254.53	4.617	296	72.5	70.0
249.23	4.128	256	67.0	65.0
244.03	3.772	225	62.0	60.0
240.00	3.460	196	57.0	55.5
233.37	3.089	173	52.0	50.5
228.90	2.833	152	49.0	48.0
224.10	2.590	131	46.0	45.0
219.47	2.432	105	44.0	43.0
215.10	2.267	94	42.5	41.5
209.03	2.073	79	39.0	38.5
204.03	1.947	85	38.0	37.5
194.30	1.765	66	37.0	36.5
190.63	1.706	65	36.0	35.5
184.37	1.612	61	35.0	34.5
178.23	1.534	60	34.0	33.5
39.60	0.349	5.46	25.8	25.6
17.17	0.181	1.25	24.9	24.7

Table B4.1 No-load results at synchronous speed for the 2.2 kW cage induction machine

The machine was first run for more than one hour, in order to allow a state of thermal equilibrium to be reached. In addition, delays between readings were incorporated for this same purpose. The maximum voltage used corresponded to rated current. Readings were taken throughout the saturation range and also at fluxes close to that of the locked-rotor and rotor removed results. The results are presented in table B4.1. No-load tests at synchronous speed were also carried out for the 6 kW wound rotor machine, these are presented in table B4.2.

$V_{average}$ (Volts)	$I_{average}$ (Amps)	W (Watts)
304.25	9.896	1005
299.50	9.171	870
294.25	8.553	790
289.50	7.930	688
284.00	7.361	609
279.75	6.836	545
274.00	6.273	495
269.25	5.889	410
264.25	5.436	402
259.75	5.118	358
254.50	4.766	325
250.00	4.498	295
244.50	4.142	259
239.50	3.883	255
234.50	3.646	215
229.50	3.415	196
224.50	3.227	162
219.50	3.123	145

Table B4.2 No-load results at synchronous speed for the 6 kW wound rotor induction machine

APPENDIX B5
Full load test results

The full-load test was carried out at rated voltage and current. The induction machine was loaded using a d.c. generator and once the thermocouple readings had stabilised the electrical input was measured using a three-phase wattmeter and the slip determined by means of a stroboscope synchronised to the motor supply. Results are given in table B5.1.

$V_{average}$ (Volts)	$I_{average}$ (Amps)	W (Watts)	VA (Volt-Amps)	Slip	T1 (°C)	T2 (°C)
247.03	4.645	2285	3415	0.024	79.1	76.2

Table B5.1 Full-load test results

A cooling curve was used to determine the d.c. resistance.

APPENDIX B6
Calculation of a.c. stator resistance

From table B2.1 the combined a.c. stator resistance and core-loss was 3.56 Ω compared to a d.c. resistance, at the same temperature, of 3.482 Ω. In order to seperate the core-loss, a no-load test, at synchronous speed and approximately the same flux level, was performed, as given in the bottom line of table B4.1. The stator resistance losses must be subtracted from power dissipated in order to obtain the core-loss. Using the room temperature resistance value, from table B1.1, and adjusting for the test temperature, the power dissipated in the stator resistance is 0.29 W. Hence, the core-loss is 0.96 W, and the a.c. resistance, for the rotor removed test, is 3.534 Ω. This value is 1.5% higher than the d.c. resistance value.

APPENDIX B7

Variation of the magnetising parameters with voltage

V(Volts)	E(Volts)	Xp(Ω)	Rp(Ω)
258.73	237.86	47.56	2026
254.53	235.33	50.98	2288
249.23	232.05	56.24	2009
244.03	228.33	60.56	1941
240.00	225.62	65.24	2000
233.37	220.51	71.44	1843
228.90	217.11	76.70	1916
224.10	213.34	82.44	2061
219.47	209.35	86.16	1983
215.10	205.70	90.81	2268
209.03	200.42	96.77	2262
204.03	195.98	100.73	2664
201.37	193.47	101.93	2199
194.30	187.01	106.03	2850
190.63	183.57	107.69	2673
184.37	177.69	110.33	2573
178.23	171.85	112.16	2321

Table B7.1 Variation of Xp and Rp with voltage for the 2.2 kW machine

APPENDIX B8

Calculation of rotor resistance under normal frequency conditions

From the test results, see appendix B5, the phase angle, ϕ, in figure B8.1, can be calculated. Also, since the stator resistance and leakage reactance are known the stator voltage drop, Vs, and phase angle, θ, can be calculated. By trigonometry, it can be shown that the voltage, E, across the magnetising parameters is related to V and V_S by:

$$E^2 = \{V - V_s\cos(\theta - \phi)\}^2 + \{V_s\sin(\theta - \phi)\}^2 \qquad \text{.......... B8.1}$$

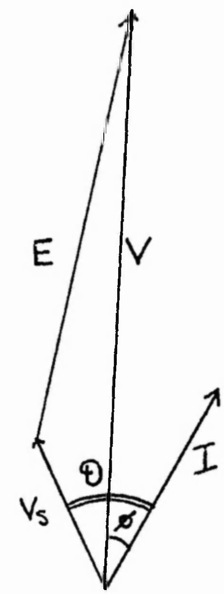


Figure B8.1 Phasor diagram used to determine the magnetising voltage

From this formula, E can be calculated and hence, using the results of table B7.1, R_p and X_p can be obtained. The rotor resistance can then be determined since it is the only remaining unknown. A computer program, listed in appendix F, was written for this purpose.

APPENDIX C

THE DETERMINATION OF THE ROOTS OF A SET OF SIMULTANEOUS EQUATIONS BY MEANS OF THE NEWTON-RAPHSON METHOD

The Newton-Raphson method is an iterative technique used to determine the roots of equations. It is based upon the Taylor series expansion. Consider the following equation:-

$$f(x) = a + bx + cx^2 + dx^3 = 0 \qquad \text{........C.1}$$

The Taylor series for f(x) about a point $x = x_0$ is defined as:-

$$f(x) = f(x_0) + f'(x_0)(x - x_0) + f''(x_0)\frac{(x - x_0)^2}{2!} + \ldots \qquad \text{........C.2}$$

If prior knowledge is available regarding an approximate value for one of the roots of f(x) then this value can be selected for x_0 so as to make the term $\{x-x_0\}$ small. The higher order terms of the Taylor series will become increasingly insignificant and therefore an approximate version of the Taylor series can be obtained by truncating the series after the first derivative term:-

$$f(x) = f(x_0) + f'(x_0)(x - x_0) \qquad \text{........C.3}$$

Applying equation C.1, equation C.3 can be rearranged as:-

$$x = x_0 - \frac{f(x_0)}{f'(x_0)} \qquad \text{........C.4}$$

Because this equation is an approximation, based upon ignoring the higher order terms of the series, this process should be repeated until sufficient accuracy is achieved. For each repetition the new value used for x_0 is the previous solution of equation C.4.

The Newton-Raphson method can also be used to solve simultaneous equations involving more than one variable. Consider the case of three non-linear equations involving three unknowns:-

$$f(x,y,z) = 0 \qquad \text{........C.5}$$

$$g(x,y,z) = 0 \qquad \text{........C.6}$$

$$h(x,y,z) = 0 \qquad \text{........C.7}$$

Provided that a set of values x_0, y_0, z_0 are known, that are near solutions to equations C5 to C7, then the Taylor series expansions can be simplified by ignoring terms above the first derivative term. Equation C.8 is the approximate expansion of equation C.5:-

$$f(x,y,z) = f(x_0,y_0,z_0) + f_x(x_0,y_0,z_0).(x - x_0) + f_y(x_0,y_0,z_0).(y - y_0) + f_z(x_0,y_0,z_0).(z - z_0) \qquad \text{........C.8}$$

where f_x is the partial derivative of f with respect to x, etc.

Similar expansions can be performed with equations C6 and C7 and these three equations can then be rearranged as follows:-

$$A1.(x - x_0) + B1.(y - y_0) + C1.(z - z_0) = D1 \qquad \text{...... C.9}$$

$$A2.(x - x_0) + B2.(y - y_0) + C2.(z - z_0) = D2 \qquad \text{......C.10}$$

$$A3.(x - x_0) + B3.(y - y_0) + C3.(z - z_0) = D3 \qquad \text{......C.11}$$

Where

$$f_x(x_0,y_0,z_0) = A1$$

$$f_y(x_0,y_0,z_0) = B1$$

$$f_z(x_0,y_0,z_0) = C1$$

$$-f(x_0,y_0,z_0) = D1$$

and similarly for g_x, h_x etc.

By Cramers Rule [88], equations C9 to C11 can be solved as follows:-

$$\{x - x_0\} = \frac{\begin{vmatrix} D1 & B1 & C1 \\ D2 & B2 & C2 \\ D3 & B3 & C3 \end{vmatrix}}{\begin{vmatrix} A1 & B1 & C1 \\ A2 & B2 & C2 \\ A3 & B3 & C3 \end{vmatrix}} \qquad \text{......C.12}$$

$$\{y - y_0\} = \frac{\begin{vmatrix} A1 & D1 & C1 \\ A2 & D2 & C2 \\ A3 & D3 & C3 \end{vmatrix}}{\begin{vmatrix} A1 & B1 & C1 \\ A2 & B2 & C2 \\ A3 & B3 & C3 \end{vmatrix}} \qquad \text{......C.13}$$

$$\{z - z_0\} = \frac{\begin{vmatrix} A1 & B1 & D1 \\ A2 & B2 & D2 \\ A3 & B3 & D3 \end{vmatrix}}{\begin{vmatrix} A1 & B1 & C1 \\ A2 & B2 & C2 \\ A3 & B3 & C3 \end{vmatrix}} \qquad \text{......C.14}$$

As with the one variable case, the values of x, y and z should be used as x_0, y_0 and z_0 for each subsequent calculation, until sufficient accuracy is achieved.

APPENDIX D

MACHINE PARAMETER DETERMINATION PROGRAM LISTING

```
PROGRAM Determine_Characteristics (input,output);

  VAR   V_nl, V_lr          : Real;
        I_nl, I_lr          : Real;
        W_nl, W_lr          : Real;
        Angle               : Real;
        Rbr,Xbr             : Real;
        R1, R2, R1_nl       : Real;
        X1, Rp, Xp          : Real;
        RL, XL, Rms, Xms    : Real;
        A, B, C, D, E, F    : Real;
        F1, F2              : Real;
        D_X1, D_R1          : Real;
        D_X2, D_R2          : Real;
        PATH_CHOICE, J      : Integer;
        V_e                 : Real;

  BEGIN
    WRITELN('DO YOU WISH TO INPUT (A) LOCKED ROTOR & NO LOAD RESULTS');
    WRITELN('OR (B) NO LOAD RESULTS WITH A VALUE OF X1 ?');
    WRITELN('TYPE (0) FOR (A) OR (1) FOR (B): ');
    READ(PATH_CHOICE);
    WRITELN('INPUT NO LOAD RESULTS');
    WRITE('VOLTAGE: ');
    READ(V_nl);
    WRITE('PHASE CURRENT: ');
    READ(I_nl);
    WRITE('POWER: ');
    READ(W_nl);
    WRITELN('');
    IF PATH_CHOICE = 0 THEN
    BEGIN
      WRITELN('INPUT LOCKED ROTOR RESULTS');
      WRITE('VOLTAGE: ');
      READ(V_lr);
      WRITE('PHASE CURRENT: ');
      READ(I_lr);
      WRITE('POWER: ');
      READ(W_lr);
      WRITELN;
      WRITELN;
      WRITE('INPUT STATOR RESISTANCE RESULT FOR LOCKED ROTOR: ');
      READ(R1)
    END;
    WRITE('INPUT STATOR RESISTANCE RESULT FOR NO LOAD: ');
    READ(R1_nl);
    WRITELN;
    Angle := arctan( Sqrt(Sqr(V_nl*I_nl*3) - Sqr(W_nl)) / W_nl);
    Rms := (V_nl / I_nl ) * cos(Angle) - R1_nl;
    XL := (V_nl / I_nl ) * sin(Angle);
    IF PATH_CHOICE = 0 THEN
    BEGIN
      Angle := arctan( Sqrt(Sqr(V_lr*I_lr*3) - Sqr(W_lr)) / W_lr);
      Rbr := (V_lr / I_lr ) * cos(Angle);
      Xbr := (V_lr / I_lr ) * sin(Angle)
    END;
    IF PATH_CHOICE = 1 THEN
    BEGIN
      WRITE('INPUT STATOR LEAKAGE REACTANCE: ');
      READ(X1);
      Xms := XL - X1
    END
    ELSE
    BEGIN
      R2 := 10;
      X1 := 10;
      FOR J := 1 TO 6 DO
      BEGIN
```

```
      A:= R1 - Rbr + Rms;
      B:= 2 * Rms * ( R1 - Rbr ) + Rms*Rms + XL*XL + X1*X1 - 2*XL*X1;
      C:= ( Rms*Rms + XL*XL )*( R1 - Rbr ) + X1*X1*Rms;
      F1 := A*R2*R2 + B*R2 + C;
      D:= XL - Xbr;
      E:= 2*Rms*(X1 - Xbr);
      F:= ( 2*X1 - Xbr )*( XL*XL + Rms*Rms ) - X1*X1*XL;
      F2 := D*R2*R2 + E*R2 + F;
      D_X1 := 2*R2*( X1 - XL ) + 2*X1*Rms;
      D_R1 := 2*R2*A + B;
      D_X2 := 2*Rms*R2 + 2*( XL*XL + Rms*Rms ) - 2*X1*XL;
      D_R2 := 2*R2*D + E;
      R2 := R2 + (((D_X2 / D_X1)*(-F1) + F2) /  ((D_X2 / D_X1)*D_R1 - D_R2));
      X1 := X1 + (((D_R2 / D_R1)*(-F1) + F2) / ((D_R2 / D_R1)*D_X1 - D_X2));
      WRITELN('RESULTS NUMBER ',J,' X1 = ',X1,' R2 = ',R2)
    END;
    Xms := XL - X1;
  END;
  WRITELN;
  Rp := Rms + (Xms*Xms)/Rms;
  Xp := Xms + (Rms*Rms)/Xms;
  WRITELN('CALCULATED VALUE OF Rms IS ',Rms,' Ohms');
  WRITELN('    "         "    "  Rp  "  ',Rp,' Ohms');
  WRITELN('CALCULATED VALUE OF Xms IS ',Xms,' Ohms');
  WRITELN('    "         "    "  Xp  "  ',Xp,' Ohms');
  IF PATH_CHOICE = 0 THEN
    BEGIN
      WRITELN('CALCULATED VALUE OF X1 & X2 IS ',X1,' Ohms');
      WRITELN('CALCULATED VALUE OF R2 IS ',R2,' Ohms')
    END;
  V_e := V_nl*(Sqrt(Rms*Rms+Xms*Xms)/Sqrt(Sqr(R1_nl+Rms)+Sqr(X1+Xms)));
  WRITELN('VOLTAGE ACROSS MAGNETISING BRANCH IS : ',V_e,' Volts')
END.
```

APPENDIX E

GENERALISED LINEAR INTERACTIVE MODELLING (GLIM)

A model, of the form presented in equation E.1, is linear provided that the parameters, β_0, $\beta_1 \ldots \beta_n$, have fixed values [89].

$$Y = \beta_0 + \beta_1 X + \beta_2 X^2 + \beta_3 X^3 + \ldots \beta_n X^n + \qquad \ldots\ldots\ldots E.1$$

The NAG package GLIM [90] can be used to fit models, of this type to a set of data. It uses a least squares fit approach to determine the best values for the parameters of the model chosen. The GLIM package is interactive, thus enabling models to be built up and appraised. The goodness of fit of the model is assessed according to it's coefficient of determination r^2 [91]:

$$r^2 = \frac{\{\text{explained variation}\}}{\{\text{total variation}\}} \qquad \ldots\ldots\ldots E.2$$

$$\text{Total variation} = (Y_m - Y)^2 \qquad \ldots\ldots\ldots E.3$$

$$\text{Explained variation} = (Y_{est} - Y)^2 \qquad \ldots\ldots\ldots E.4$$

$$\text{Unexplained variation} = (Y_m - Y_{est})^2 \qquad \ldots\ldots\ldots E.5$$

Where Y_m = measured (experimental) result

Y_{est} = model's estimate of the Y variable

Y = mean of experimental results

For a perfect fit the total variation will equal the explained variation and, hence, the coefficient of determination will equal one. Where there is no correlation all the variation will be unexplained and the coefficient of determination will be zero.

The coefficient of correlation gives a measure of the goodness of fit of the model to the data. Further information regarding the validity of the model can be gained by examining the distribution of the residuals. The residuals are the error terms, the differences between the measured and estimated results. If when plotted against X these terms form an approximately horizontal band then the model is satisfactory, since the errors appear to be random. However, any pattern to the residuals indicates that the model is incomplete.

By continuously adding additional terms to the model the explained variation will increase and hence the coefficient of determination will increase. However, this is not the best approach since by using a simple model the relationship will be easier to understand and the model will be less influenced by peculiarities within the sample. The method used to determine the significance of adding a further term to a model was a comparison between the estimate for the parameter and its standard error. According to the GLIM manual [90], a parameter estimate less than its standard error will be insignificant, and one more than three times it's standard error usually significant. These criteria, along with an examination of the residuals, were used to obtain suitable models that closely fitted experimental data.

APPENDIX F

ROTOR RESISTANCE CALCULATION PROGRAM LISTING

```
PROGRAM R2 (Input,Output);

VAR V, I, R1    : Real;
    X1, X2, Rc  : Real;
    Rm, Xc, Xm  : Real;
    S, E, Ze    : Real;
    Rp, Xp      : Real;
    A, B, C, G  : Real;
    H, J, K, L  : Real;
    M           : Real;
    Root_1      : Real;
    Root_2      : Real;
    Cont        : Real;

BEGIN
  WRITE('Enter R1 : ');
  READ(R1);
  WRITE('Enter X1 : ');
  READ(X1);
  WRITE('Enter X2 : ');
  READ(X2);
  WRITE('Enter Rc : ');
  READ(Rc);
  WRITE('Enter Rm : ');
  READ(Rm);
  WRITE('Enter Xc : ');
  READ(Xc);
  WRITE('Enter Xm : ');
  READ(Xm);
  REPEAT
    WRITE('Enter V : ');
    READ(V);
    WRITE('Enter Phase Current : ');
    READ(I);
    WRITE('Enter Slip : ');
    READ(S);
    E := V - SQRT(R1*R1 + SQR(X1*(1-S)))*I;
    Ze := E/I;
    Rp := Rc + Rm*E;
    Xp := Xc + Xm*E;
    G := Rp/S;
    H := -Xp*X2*SQR(1-S);
    J := (Xp*(1-S))/S;
    K := Rp*(Xp + X2)*(1-S);
    L := SQR(SQR(1-S)*X2*Xp*Rp);
    M := SQR((1-S)*Xp*Rp/S);
    A := Ze*Ze*(G*G + J*J) - M;
    B := 2*Ze*Ze*(G*H + J*K);
    C := Ze*Ze*(H*H + K*K) - L;
    Root_1 := ((-B) + SQRT(B*B - 4*A*C)) / (2*A);
    Root_2 := ((-B) - SQRT(B*B - 4*A*C)) / (2*A);
    WRITELN('ROOT 1 = ',Root_1,'  ROOT 2 = ',Root_2);
    WRITE('ANOTHER SET OF RESULTS ? (0) NO, (1) YES : ');
    READ(Cont)
  UNTIL Cont = 0
END.
```

APPENDIX G

COMPUTER MODEL PROGRAM LISTING

```
PROGRAM Series_shunt_system(Input,Output,FX1,FX2,FX3);

VAR FX1, FX2, FX3    : FILE OF Real;
    Choice, It, J    : Integer;
    R1, R2, Rc, Rm   : Real;
    X1, X2, Xc, Xm   : Real;
    Cs, Cp, pf       : Real;
    Pturb, Wmax, Kt  : Real;
    Pgen, Kg         : Real;
    K0, K1, K2, K3   : Real;
    K4, K5, K6, K7   : Real;
    K8, K9, K10      : Real;
    K11, K12         : Real;
    E, S, W          : Real;
    Cs_init, Cp_init: Real;
    S_Init, X1_Init : Real;
    E_init, W_Init  : Real;
    N1, N2           : Integer;
    L1, L2, Lc, Lm   : Real;
    XCs, XCp         : Real;
    R, X, Xz, V, Z   : Real;
    A, B, C, D, F    : Real;
    A_e, A_s, A_w    : Real;
    B_e, B_s, B_w    : Real;
    C_e, C_s, C_w    : Real;
    D_e, D_s, D_w    : Real;
    E_e, E_s, E_w    : Real;
    F_e, F_s, F_w    : Real;
    X1_e, X1_s, X1_w: Real;
    Xz_e, Xz_s, Xz_w: Real;
    XCp_e, XCp_s     : Real;
    XCp_w            : Real;
    de, ds, dw, dc   : Real;
    A1, B1, C1, D1   : Real;
    A2, B2, C2, D2   : Real;
    A3, B3, C3, D3   : Real;
    Pt, Pw, Pr, Ps   : Real;
    Pc, DPr          : Real;
    Pt_e, Pt_s, Pt_w: Real;
    Pw_e, Pw_s, Pw_w: Real;
    Pr_e, Pr_s, Pr_w: Real;
    Ps_e, Ps_s, Ps_w: Real;
    Pc_e, Pc_s, Pc_w: Real;
    Pi               : Real;
    Rmax, Imax, Rpar: Real;
    Vgen, Vload, I   : Real;
    Decision         : Integer;
    Graph_type       : Integer;
    Q, Qmax          : Integer;
    Path_Choice      : Integer;
    Y_max, Y_min     : Real;
    X_max, Y_orig    : Real;
    Y_co, X_co       : Real;
    PAIR, EXPTL      : Integer;
    OVER, ORIG       : Integer;
    XP, YP           : ARRAY [1..26] OF Real;

    NAMFIL : PACKED ARRAY [1..128] OF CHAR;
    STRING : PACKED ARRAY [1..128] OF CHAR;
    IENABL, ISIZE, ISTART, ISTOP, ICODE, METHOD, COLOUR : INTEGER;
    XLEFT, XRIGHT, YLOWER, YUPPER, XDIV, YDIV, XPOS, YPOS : INTEGER;
    NDASH1, NDASH2, NGAP1, NGAP2, NCHARS, KCHAR : INTEGER;

PROCEDURE PAPER(%REF IENABLE : INTEGER);FORTRAN;
PROCEDURE FILNAM(%STDESCR NAMFIL : PACKED ARRAY [LO..HI:INTEGER] OF CHAR);
                 FORTRAN;
PROCEDURE MAP(%REF XLEFT, XRIGHT, YLOWER, YUPPER : Real);FORTRAN;
PROCEDURE CTRMAG(%REF ISIZE : INTEGER);FORTRAN;
```

```
PROCEDURE PSPACE(%REF XLEFT, XRIGHT, YLOWER, YUPPER : Real);FORTRAN;
PROCEDURE AXESSI(%REF XDIV, YDIV : Real);FORTRAN;
PROCEDURE AXORIG(%REF ORIGX, ORIGY  : Real);FORTRAN;
PROCEDURE PTPLOT(%REF XP:ARRAY [LO..HI : INTEGER] OF Real;
                 %REF YP:ARRAY [L..H : INTEGER] OF Real;
                 %REF ISTART, ISTOP, ICODE : INTEGER);FORTRAN;
PROCEDURE CURVEO(%REF XP:ARRAY [LO..HI : INTEGER] OF Real;
                 %REF YP:ARRAY [L..H : INTEGER] OF Real;
                 %REF ISTART, ISTOP : INTEGER);FORTRAN;
PROCEDURE PCSCEN(%REF XPOS, YPOS : Real;
                 %STDESCR STRING : PACKED ARRAY [LO..HI : INTEGER] OF CHAR);
                 FORTRAN;
PROCEDURE LINCOL(%REF COLOUR : INTEGER);FORTRAN;
PROCEDURE BROKEN(%REF NDASH1, NGAP1, NDASH2, NGAP2 : INTEGER);FORTRAN;
PROCEDURE FULL; FORTRAN;
PROCEDURE TYPECS(%STDESCR STRING : PACKED ARRAY [LO..HI : INTEGER] OF CHAR;
                 %REF NCHARS : REAL);FORTRAN;
PROCEDURE PLOTNC(%REF XPOS, YPOS : REAL;
                 %REF KCHAR : INTEGER);FORTRAN;
PROCEDURE JOIN(%REF XPOS, YPOS : REAL);FORTRAN;
PROCEDURE POSITN(%REF XPOS, YPOS : REAL);FORTRAN;
PROCEDURE FRAME;FORTRAN;
PROCEDURE GREND;FORTRAN;
PROCEDURE AXES;FORTRAN;

PROCEDURE Assign(VAR XX : Real);

BEGIN
  CASE Choice OF
    0 : ;
    1 : BEGIN
          XX := FX1^;
          GET(FX1)
        END;
    2 : BEGIN
          XX := FX2^;
          GET(FX2)
        END;
    3 : BEGIN
          XX := FX3^;
          GET(FX3)
        END
  END
END;

PROCEDURE Display_Parameters;

BEGIN
  WRITELN('R1 = ',R1,' Ohms');
  WRITELN('R2 = ',R2,' Ohms');
  WRITELN('Rc = ',Rc,' Ohms');
  WRITELN('Rm = ',Rm,' Ohms/Volt');
  WRITELN('X1 = ',X1_init,' Ohms at 50Hz');
  WRITELN('X2 = ',X2,' Ohms at 50Hz');
  WRITELN('Xc = ',Xc,' Ohms at 50Hz');
  WRITELN('Xm = ',Xm,' Ohms/Volt at 50Hz');
  WRITELN('Cs = ',Cs_init,' uF');
  WRITELN('Cp = ',Cp_init,' uF');
  WRITELN('Power Factor = ',pF);
  WRITELN('Turbine Power Pturb = ',Pturb,' W');
  WRITELN('Frequency for Max Turbine Power Wmax = ',Wmax,' Rad/s');
  WRITELN('Turbine speed coefficient K0 = ',K0);
  WRITELN('    "       "         "       K1 = ',K1);
  WRITELN('    "       "         "       K2 = ',K2);
  WRITELN('    "       "         "       K3 = ',K3);
  WRITELN('    "       "         "       K4 = ',K4);
  WRITELN('    "       "         "       K5 = ',K5);
  WRITELN('    "       "         "       K6 = ',K6);
```

```
  WRITELN('    "       "         "        K7 = ',K7);
  WRITELN('    "       "         "        K8 = ',K8);
  WRITELN('    "       "         "        K9 = ',K9);
  WRITELN('    "       "         "        K10 = ',K10);
  WRITELN('    "       "         "        K11 = ',K11);
  WRITELN('    "       "         "        K12 = ',K12);
  WRITELN('Turbine Friction and Windage Factor Kt = ',Kt);
  WRITELN('Generator Power Pgen = ',Pgen,' W');
  WRITELN('Generator Friction and Windage Factor Kg = ',Kg);
  WRITELN('Initial estimate for Magnetising Voltage E = ',E_init,' Volts');
  WRITELN('Initial estimate for Slip S = ',S_init);
  WRITELN('Initial estimate for Turbine frequency W = ',W_init,'Rad/s')
END;

PROCEDURE Coeffs1(A_t,B_t,C_t,D_t,F_t,X1_t,Xz_t:Real; VAR T1:Real);

VAR Num1, Den1       : Real;
    Num2, Den2       : Real;
    Num1_t, Den1_t   : Real;
    Den2_t           : Real;

BEGIN
  Num1  := F * (R1*F + B*D - A*C);
  Den1  := Sqr(R1*F + B*D - A*C) + Sqr(X1*F + B*C + A*D);
  Num2  := R;
  Den2  := R*R + Xz*Xz;
  Num1_t  := F_t*(R1*F + B*D - A*C) + F*(R1*F_t + B_t*D + B*D_t - A_t*C - A*C_t);
  Den1_t  := 2*(R1*F + B*D - A*C)*(R1*F_t + B_t*D + B*D_t - A_t*C - A*C_t) +
           2*(X1*F + B*C + A*D)*(X1*F_t + X1_t*F + B_t*C + B*C_t + A_t*D + A*D_t);
  Den2_t  := 2*Xz*Xz_t;
  T1  := (Num1_t - Num1/Den1*Den1_t)/(Den1) - Num2*Den2_t/(Sqr(Den2))
END;

PROCEDURE Coeffs2(A_t,B_t,C_t,D_t,F_t,X1_t,Xz_t,XCp_t:Real; VAR T2:Real);

VAR Num1, Den1       : Real;
    Num2, Den2       : Real;
    Num1_t, Den1_t   : Real;
    Num2_t, Den2_t   : Real;

BEGIN
  Num1  := F * (X1*F + B*C + A*D);
  Den1  := Sqr(R1*F + B*D - A*C) + Sqr(X1*F + B*C + A*D);
  Num2  := -Xz;
  Den2  := R*R + Xz*Xz;
  Num1_t  := F_t*(X1*F + B*C + A*D) +
             F*(X1_t*F + X1*F_t + B_t*C + B*C_t + A_t*D + A*D_t);
  Den1_t  := 2*(R1*F + B*D - A*C)*(R1*F_t + B_t*D + B*D_t - A_t*C - A*C_t) +
           2*(X1*F + B*C + A*D)*(X1*F_t + X1_t*F + B_t*C + B*C_t + A_t*D + A*D_t);
  Num2_t  := -Xz_t;
  Den2_t  := 2*Xz*Xz_t;
  T2  := (Num1_t - Num1/Den1*Den1_t)/(Den1) +
         (Den2*Num2_t - Num2*Den2_t)/(Sqr(Den2)) - XCp_t/(Sqr(XCp))
END;

PROCEDURE PCoeffs3(A_t,B_t,C_t,D_t,E_t,F_t:Real; VAR Ps_t:Real);

VAR Num, Den       : Real;
    Num_t, Den_t   : Real;

BEGIN
  Num  := 3*E*E*R1*F*F;
  Den  := Sqr(B*C + A*D) + Sqr(B*D - A*C);
  Num_t  := 3*R1*(2*E*E_t*F*F + 2*F*F_t*E*E);
  Den_t  := 2*(B*C + A*D)*(B_t*C + C_t*B + A_t*D + D_t*A) +
            2*(B*D - A*C)*(B_t*D + D_t*B - A_t*C - C_t*A);
  Ps_t  := (Num_t - Num/Den*Den_t)/(Den)
END;
```

```
FUNCTION Determinant (Z1,Z2,Z3,Z4,Z5,Z6,Z7,Z8,Z9 : Real) : Real;
BEGIN
  Determinant := Z1*Z5*Z9 + Z2*Z6*Z7 + Z3*Z4*Z8 - Z3*Z5*Z7 - Z6*Z8*Z1 -
                 Z9*Z2*Z4
END;

BEGIN
  Pi := 4 * Arctan(1.0);
  WRITELN('Enter File choice 1, 2 or 3');
  READ(Choice);
  CASE Choice OF
    0 : ;
    1 : RESET(FX1);
    2 : RESET(FX2);
    3 : RESET(FX3)
  END;
  assign(R1);
  assign(R2);
  assign(Rc);
  assign(Rm);
  assign(X1_init);
  assign(X2);
  assign(Xc);
  assign(Xm);
  assign(Cs_init);
  assign(Cp_init);
  assign(pf);
  assign(Pturb);
  assign(Wmax);
  assign(K0);
  assign(K1);
  assign(K2);
  assign(K3);
  assign(K4);
  assign(K5);
  assign(K6);
  assign(K7);
  assign(K8);
  assign(K9);
  assign(K10);
  assign(K11);
  assign(K12);
  assign(Kt);
  assign(Pgen);
  assign(Kg);
  assign(E_init);
  assign(S_init);
  assign(W_init);
  Display_Parameters;
  WRITELN('');
  WRITELN('DO YOU WANT TO PLOT LOAD VOLTAGE, GENERATOR VOLTAGE, ');
  WRITELN('MAGNETISING VOLTAGE, GENERATED FREQUENCY, SLIP,');
  WRITELN('TURBINE FREQUENCY, EFFICIENCY OR GENERATED POWER vs');
  WRITELN('LOAD CURRENT ? OR LOAD VOLTAGE vs GENERATED POWER ?');
  WRITELN(' TYPE :');
  WRITELN('(1) FOR LOAD VOLTAGE');
  WRITELN('(2) FOR GENERATOR VOLTAGE');
  WRITELN('(3) FOR MAGNETISING VOLTAGE');
  WRITELN('(4) FOR GENERATED FREQUENCY');
  WRITELN('(5) FOR SLIP');
  WRITELN('(6) FOR TURBINE RPM');
  WRITELN('(7) FOR EFFICIENCY');
  WRITELN('(8) FOR GENERATED POWER');
  WRITELN('(9) FOR LOAD VOLTAGE vs GENERATED POWER');
  WRITELN('(10) FOR LOAD VOLTAGE vs 1/R');
  READ(Graph_type);
  WRITELN('DO YOU WISH TO SEE INTERIM ITERATION RESULTS ?');
```

```
WRITELN('TYPE (0) FOR NO, (1) FOR YES');
READ(It);
Y_max := -1000000;
Y_min :=  1000000;
X_max :=  0;
WRITELN('DO YOU WANT A TABLE AS WELL AS A GRAPH ?');
WRITELN('TYPE (0) FOR NO, (1) FOR YES');
READ(Path_Choice);
WRITELN('ENTER NUMBER OF PLOTS : ');
READ(Qmax);
FOR Q := 1 TO Qmax DO
BEGIN
  REPEAT
    WRITELN('DO YOU WISH TO REVIEW THE LIST OF PARAMETERS ?');
    WRITELN('TYPE (0) FOR NO, (1) FOR YES.');
    READ(Decision);
    IF Decision = 1 THEN Display_Parameters;
    WRITELN('DO YOU WISH TO CHANGE ANY OF THE PARAMETERS ?  TYPE :');
    WRITELN(' (0) FOR NO');
    WRITELN(' (1) FOR R1');
    WRITELN(' (2) FOR R2');
    WRITELN(' (3) FOR Rc');
    WRITELN(' (4) FOR Rm');
    WRITELN(' (5) FOR X1');
    WRITELN(' (6) FOR X2');
    WRITELN(' (7) FOR Xc');
    WRITELN(' (8) FOR Xm');
    WRITELN(' (9) FOR Cs');
    WRITELN(' (10) FOR Cp');
    WRITELN(' (11) FOR pf');
    WRITELN(' (12) FOR Pturb');
    WRITELN(' (13) FOR Wmax');
    WRITELN(' (14) FOR K0');
    WRITELN(' (15) FOR K1');
    WRITELN(' (16) FOR K2');
    WRITELN(' (17) FOR K3');
    WRITELN(' (18) FOR K4');
    WRITELN(' (19) FOR K5');
    WRITELN(' (20) FOR K6');
    WRITELN(' (21) FOR K7');
    WRITELN(' (22) FOR K8');
    WRITELN(' (23) FOR K9');
    WRITELN(' (24) FOR K10');
    WRITELN(' (25) FOR K11');
    WRITELN(' (26) FOR K12');
    WRITELN(' (27) FOR Kt');
    WRITELN(' (28) FOR Pgen');
    WRITELN(' (29) FOR Kg');
    WRITELN(' (30) FOR E');
    WRITELN(' (31) FOR S');
    WRITELN(' (32) FOR W');
    READ(Decision);
    IF DECISION <> 0 THEN
    BEGIN
      WRITE('ENTER NEW VALUE : ');
      CASE DECISION OF
        0 : ;
        1 : READ(R1);
        2 : READ(R2);
        3 : READ(Rc);
        4 : READ(Rm);
        5 : READ(X1_init);
        6 : READ(X2);
        7 : READ(Xc);
        8 : READ(Xm);
        9 : READ(Cs_init);
       10 : READ(Cp_init);
       11 : READ(pF);
```

```
          12 : READ(Pturb);
          13 : READ(Wmax);
          14 : READ(K0);
          15 : READ(K1);
          16 : READ(K2);
          17 : READ(K3);
          18 : READ(K4);
          19 : READ(K5);
          20 : READ(K6);
          21 : READ(K7);
          22 : READ(K8);
          23 : READ(K9);
          24 : READ(K10);
          25 : READ(K11);
          26 : READ(K12);
          27 : READ(Kt);
          28 : READ(Pgen);
          29 : READ(Kg);
          30 : READ(E_init);
          31 : READ(S_init);
          32 : READ(W_init)
        END
      END
    UNTIL Decision = 0;
    Cs := Cs_init/1000000;
    Cp := Cp_init/1000000;
    E := E_init;
    S := S_init;
    W := W_init;
    X1 := X1_init;
    L1 := X1/(2*Pi*50);
    L2 := X2/(2*Pi*50);
    Lc := Xc/(2*Pi*50);
    Lm := Xm/(2*Pi*50);
    Rmax := 240*240*3/Pgen;
    Imax := (1.5*Pgen)/(E*3*pf);
    FOR N1 := 1 TO 21 DO
    BEGIN
      Z := (200 * 20)/(Imax * ((N1 - 1) + 0.000001));
      X := Z * Sqrt(1 - pf*pf);
      R := Z*pf;
      Rpar := Z/pf;
      IF E >= 180 THEN
      REPEAT
        X1 := L1*W/(1-S);
        XCs := (1-S)/(W*Cs);
        Xz := XCs - X;
        XCp := (1-S)/(W*Cp);
        A := Sqr(W/(1-S))*L2*(Lc + 100*Pi*(1-S)/W*Lm*E)*(Rc + Rm*E);
        B := (W/(1-S))*(Lc + 100*Pi*(1-S)/W*Lm*E)*(Rc + Rm*E)*(R2/S);
        C := (Rc + Rm*E)*(R2/S) - Sqr(W/(1-S))*L2*(Lc + 100*Pi*(1-S)/W*Lm*E);
        D := (W/(1-S))*( (Lc + 100*Pi*(1-S)/W*Lm*E)*(Rc + Rm*E) +
             (R2/S)*(Lc + 100*Pi*(1-S)/W*Lm*E) + L2*(Rc + Rm*E) );
        F := C*C + D*D;
        A_e := Sqr(W/(1-S))*L2*(Lc*Rm + 100*Pi*(1-S)/W*(Lm*Rc + Lm*Rm*2*E));
        B_e := (W/(1-S))*(R2/S)*(Lc*Rm + 100*Pi*(1-S)/W*(Lm*Rc + Lm*Rm*2*E));
        C_e := Rm*(R2/S) - Sqr(W/(1-S))*L2*Lm*100*Pi*(1-S)/W;
        D_e := (W/(1-S))*(L2*Rm + Lc*Rm +
               100*Pi*(1-S)/W*(Lm*Rm*2*E + (R2/S)*Lm + Rc*Lm));
        F_e := 2*C*C_e + 2*D*D_e;
        X1_e := 0;
        Xz_e := 0;
        Coeffs1(A_e,B_e,C_e,D_e,F_e,X1_e,Xz_e,A1);
        A_s := 2*(1/(1-S))**3*W*W*L2*Lc*(Rc + Rm*E) +
               100*Pi*(1/(1-S))**2*W*L2*Lm*E*(Rc + Rm*E);
        B_s := (2*S - 1)*Sqr(1/(S - S*S))*W*Lc*(Rc + Rm*E)*R2 -
               Sqr(1/S)*100*Pi*Lm*E*(Rc + Rm*E)*R2;
        C_s := -R2*Sqr(1/S)*(Rc + Rm*E) - 2*(1/(1-S))**3*W*W*L2*Lc -
```

```
        Sqr(1/(1-S))*W*L2*100*Pi*Lm*E;
D_s  := (2*S - 1)*Sqr(1/(S - S*S))*W*R2*Lc -
        R2*Sqr(1/S)*100*Pi*Lm*E +
        W*Sqr(1/(1-S))*(Lc*(Rc + Rm*E) + L2*(Rc + Rm*E));
F_s  := 2*C_s*C + 2*D_s*D;
X1_s  := W*L1*Sqr(1/(1-S));
Xz_s  := -1/(W * Cs);
Coeffs1(A_s,B_s,C_s,D_s,F_s,X1_s,Xz_s,B1);
A_w  := 2*W*Sqr(1/(1-S))*L2*Lc*(Rc + Rm*E) +
        (1/(1-S))*L2*100*Pi*Lm*E*(Rc + Rm*E);
B_w  := (1/(1-S))*Lc*(Rc + Rm*E)*(R2/S);
C_w  := -2*W*Sqr(1/(1-S))*L2*Lc - (1/(1-S))*L2*100*Pi*Lm*E;
D_w  := (1/(1-S))*(Lc*(Rc + Rm*E) + (R2/S)*Lc +
        L2*(Rc + Rm*E));
F_w  := 2*C_w*C + 2*D_w*D;
X1_w  := L1*(1/(1-S));
Xz_w  := (S-1)/(W*W*Cs);
Coeffs1(A_w,B_w,C_w,D_w,F_w,X1_w,Xz_w,C1);
D1  := -F*(R1*F + B*D - A*C)/(Sqr(R1*F + B*D -A*C) +
       Sqr(X1*F + B*C + A*D)) - R / (R*R + Xz*Xz);
XCp_e  := 0;
XCp_s  := -1/(W*Cp);
XCp_w  := (S-1)/(W*W*Cp);
Coeffs2(A_e,B_e,C_e,D_e,F_e,X1_e,Xz_e,XCp_e,A2);
Coeffs2(A_s,B_s,C_s,D_s,F_s,X1_s,Xz_s,XCp_s,B2);
Coeffs2(A_w,B_w,C_w,D_w,F_w,X1_w,Xz_w,XCp_w,C2);
D2  := Xz/(R*R + Xz*Xz) + 1/XCp - F*((X1*F + B*C + A*D)/
       (Sqr(R1*F + B*D -A*C) + Sqr(X1*F + B*C + A*D)));
Pt  := Pturb*(K0 + K1*W + K2*W*W + K3*(W**3) + K4*(W**4) + K5*(W**5) +
       K6*(W**6) + K7*(W**7) + K8*(W**8) + K9*(W**9) + K10*(W**10) +
       K11*(W**11) + K12*(W**12));
Pw  := (W/Wmax)*(Kt*Pturb + Kg*Pgen);
DPr  := Sqr(R2/S) + Sqr((W*L2)/(1-S));
Pr  := -3*E*E*(R2/S)/DPr;
Ps  := 3*E*E*R1*F*F/(Sqr(B*C + A*D) + Sqr(B*D - A*C));
Pc  := 3*E*E/(Rc + Rm*E);
Pt_e  := 0;
Pw_e  := 0;
Pr_e  := 2*Pr/E;
Pc_e  := ((Rc + Rm*E)*6*E - 3*E*E*Rm)/(Sqr(Rc + Rm*E));
E_e  := 1;
PCoeffs3(A_e,B_e,C_e,D_e,E_e,F_e,Ps_e);
A3  := Pt_e - Pw_e - Pr_e - Ps_e - Pc_e;
Pt_s  := 0;
Pw_s  := 0;
Pc_s  := 0;
Pr_s  := (1/Sqr(DPr))*(DPr*3*E*E*R2/(S*S) +
         3*E*E*(R2/S)*(2*W*W*L2*L2/((1-S)**3) - 2*R2*R2/(S*S*S)));
E_s  := 0;
PCoeffs3(A_s,B_s,C_s,D_s,E_s,F_s,Ps_s);
B3  := Pt_s - Pw_s - Pr_s - Ps_s - Pc_s;
Pt_w  := Pturb*(K1 + 2*K2*W + 3*K3*W*W + 4*K4*(W**3) + 5*K5*(W**4) +
         6*K6*(W**5) + 7*K7*(W**6) + 8*K8*(W**7) + 9*K9*(W**8) +
         10*K10*(W**9) + 11*K11*(W**10) + 12*K12*(W**11));
Pw_w  := (1/Wmax)*(Kt*Pturb + Kg*Pgen);
Pr_w  := 3*E*E*(R2/S)*2*W*Sqr(L2/(1-S))/(Sqr(DPr));
Pc_w  := 0;
E_w  := 0;
PCoeffs3(A_w,B_w,C_w,D_w,E_w,F_w,Ps_w);
C3  := Pt_w - Pw_w - Pr_w - Ps_w - Pc_w;
D3  := Pw + Pr + Ps + Pc - Pt;
dc  := determinant(A1,B1,C1,A2,B2,C2,A3,B3,C3);
de  := determinant(D1,B1,C1,D2,B2,C2,D3,B3,C3) / dc;
ds  := determinant(A1,D1,C1,A2,D2,C2,A3,D3,C3) / dc;
dw  := determinant(A1,B1,D1,A2,B2,D2,A3,B3,D3) / dc;
E  := E + 1*de;
S  := S + 1*ds;
W  := W + 1*dw;
```

```
    IF It = 1 THEN WRITELN('E = ',E,' S = ',S,' W = ',W)
  UNTIL ( ABS(E/de) > 1000000) AND ( ABS(S/dS) > 1000000) AND
        ( ABS(W/dW) > 1000000);
  Vgen := E *  Sqrt( (Sqr(R1*F + B*D - A*C) + Sqr(X1*F + B*C + A*D)) /
         (Sqr(B*D - A*C) + Sqr(B*C + A*D)) );
  Vload := Vgen * Sqrt(R*R + X*X)/(Sqrt(R*R + Xz*Xz));
  I := Vgen/(Sqrt(R*R + Xz*Xz));
  IF Path_choice = 1 THEN
  BEGIN
    WRITELN('I = ',I,' E = ',E,' Vgen = ',Vgen);
    WRITELN('Vload = ',Vload,' W = ',W,' S = ',S)
  END;
  IF E >= 180 THEN
  CASE GRAPH_TYPE OF
         0 : ;
    1,9,10 : YP[N1] := Vload;
         2 : YP[N1] := Vgen;
         3 : YP[N1] := E;
         4 : YP[N1] := W/((1-S)*2*Pi);
         5 : YP[N1] := S;
         6 : YP[N1] := (W*15)/Pi;
         7 : YP[N1] := (3*I*I*R)/Pt;
         8 : YP[N1] := 3*I*I*R
  END
  ELSE YP[N1] := YP[(N1-1)];
  IF Q = 1 THEN
  BEGIN
    IF Y_max < YP[N1] THEN Y_max := YP[N1];
    IF Y_min > YP[N1] THEN Y_min := YP[N1]
  END;
  IF E >= 180 THEN
    IF (GRAPH_TYPE = 9) OR (GRAPH_TYPE = 10) THEN
    BEGIN
      IF GRAPH_TYPE = 9 THEN XP[N1] := (I*I*R)/1000;
      IF GRAPH_TYPE = 10 THEN XP[N1] := 1000/Rpar
    END
    ELSE XP[N1] := I
  ELSE XP[N1] := XP[(N1-1)];
  IF X_max < XP[N1] THEN X_max := XP[N1]
END;
IF Q = 1 THEN
BEGIN
  PAPER(1);
  FILNAM('GRAPH');
  WRITELN('DO YOU REQUIRE A FALSE Y ORIGIN?');
  WRITELN('ENTER (0) FOR NO');
  WRITELN('   "   (1) FOR YES');
  READ(ORIG);
  IF ORIG = 0 THEN Y_orig:= 0
  ELSE Y_orig:= Y_min*0.99;
  IF (GRAPH_TYPE = 9) OR (GRAPH_TYPE = 10) THEN
  MAP(0,X_max*1.005,Y_orig,Y_max*1.02)
  ELSE MAP(0,I*1.005,Y_orig,Y_max*1.02);
  CTRMAG(12);
  PSPACE(0.2,0.8,0.1,0.9);
  AXORIG(0.0,Y_orig);
  AXESSI(0,0);
  Y_co:= Y_orig + 0.6*(Y_max - Y_orig)
END;
NDASH1:= 5*Q;
NGAP1:= 5*Q;
NDASH2:= 5*((Qmax+1)-Q);
NGAP2:= 5*Q;
BROKEN(NDASH1,NGAP1,NDASH2,NGAP2);
CURVEO(XP,YP,1,21);
X_co:= X_max/20;
Y_co:= Y_co - 0.05*(Y_max-Y_orig);
POSITN(X_co,Y_co);
```

```
    X_co:= X_max/7;
    JOIN(X_co,Y_co);
    WRITELN('ENTER CURVE TITLE (40 CHARACTERS INCLUDING SPACES)');
    FOR J:= 1 TO 40 DO
    READ(STRING[J]);
    TYPECS(STRING,40)
  END;
  REPEAT
    WRITELN('DO YOU WISH TO ADD A SET OF EXPERIMENTAL RESULTS?');
    WRITELN('ENTER (0) FOR NO');
    WRITELN('   "    (240 TO 250) FOR YES (ACCORDING TO SYMBOL REQUIRED');
    READ(EXPTL);
    IF EXPTL <> 0 THEN
    BEGIN
      OVER := 1;
      PAIR := 0;
      REPEAT
        PAIR := PAIR + 1;
        WRITELN('ENTER Y CO-ORD :');
        READ(YP[PAIR]);
        WRITELN('ENTER X CO-ORD :');
        READ(XP[PAIR]);
        WRITELN('ENTER (1) FOR MORE RESULTS (0) TO FINISH :');
        READ(OVER)
      UNTIL OVER = 0;
      FULL;
      CURVEO(XP,YP,1,PAIR);
      PTPLOT(XP,YP,1,PAIR,EXPTL);
      X_co:= X_max/20;
      Y_co:= Y_co - 0.05*(Y_max - Y_orig);
      PLOTNC(X_co,Y_co,EXPTL);
      X_co:= X_max/7;
      JOIN(X_co,Y_co);
      PLOTNC(X_co,Y_co,EXPTL);
      WRITELN('ENTER CURVE TITLE (40 CHARACTERS INCLUDING SPACES)');
      FOR J:= 1 TO 40 DO
      READ(STRING[J]);
      TYPECS(STRING,40)
    END
  UNTIL EXPTL = 0;
  CASE Graph_type OF
    0 : ;
    1 : PCSCEN(I/2,Y_max*1.01,'LOAD VOLTAGE (Volts) vs PHASE CURRENT (Amps)');
    2 : PCSCEN(I/2,Y_max*1.01,'GEN VOLTAGE (Volts) vs PHASE CURRENT (Amps)');
    3 : PCSCEN(I/2,Y_max*1.01,'E (Volts) vs PHASE CURRENT (Amps)');
    4 : PCSCEN(I/2,Y_max*1.01,'GEN FREQ (Hz) vs PHASE CURRENT (Amps)');
    5 : PCSCEN(I/2,Y_max*1.01,'SLIP vs PHASE CURRENT (Amps)');
    6 : PCSCEN(I/2,Y_max*1.01,'TURBINE RPM vs PHASE CURRENT (Amps)');
    7 : PCSCEN(I/2,Y_min+0.1,'EFFICIENCY vs PHASE CURRENT (Amps)');
    8 : PCSCEN(I/2,Pgen*0.1,'GEN POWER (W) vs PHASE CURRENT (Amps)');
    9 : PCSCEN(Pgen/6000,Y_max*1.01,'LOAD VOLTAGE (Volts) vs PHASE POWER (kW)');
   10 : PCSCEN(X_max/2,Y_max*1.01,'LOAD VOLTAGE (Volts) vs CONDUCTANCE (mS)')
  END;
  FRAME;
  GREND
END.
```

APPENDIX H

TEST RIG FOR MEASURING PUMP AS TURBINE CHARACTERISTICS

An existing experimental set up was used in the Polytechnic's fluids laboratory. Water was supplied by means of two 11 kW pumps and the head adjusted by manual control of gate valves. The head across the pump as turbine was measured by means of two pressure gauges, as shown in figure H.1 [92]. The flow rate was obtained by measuring the pressure drop across an orifice plate, by means of a differential pressure transducer.

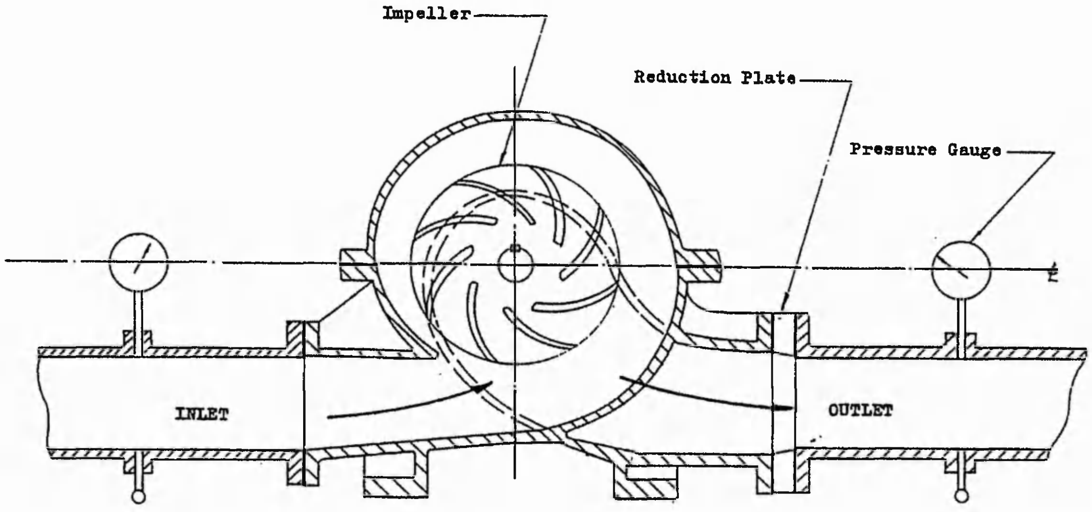


Figure H.1 Method of head measurement

The turbine was mechanically loaded by means of a cord wrapped around a friction drum and attached to a weight carrier, as shown in figure H.2 [92]. The loading was measured by means of a spring balance. The speed was measured by a hand held tachometer.

By adjusting the gate valves and varying the load on the weight carrier a range of power-speed and efficiency speed characteristics were obtained at different turbine operating heads.

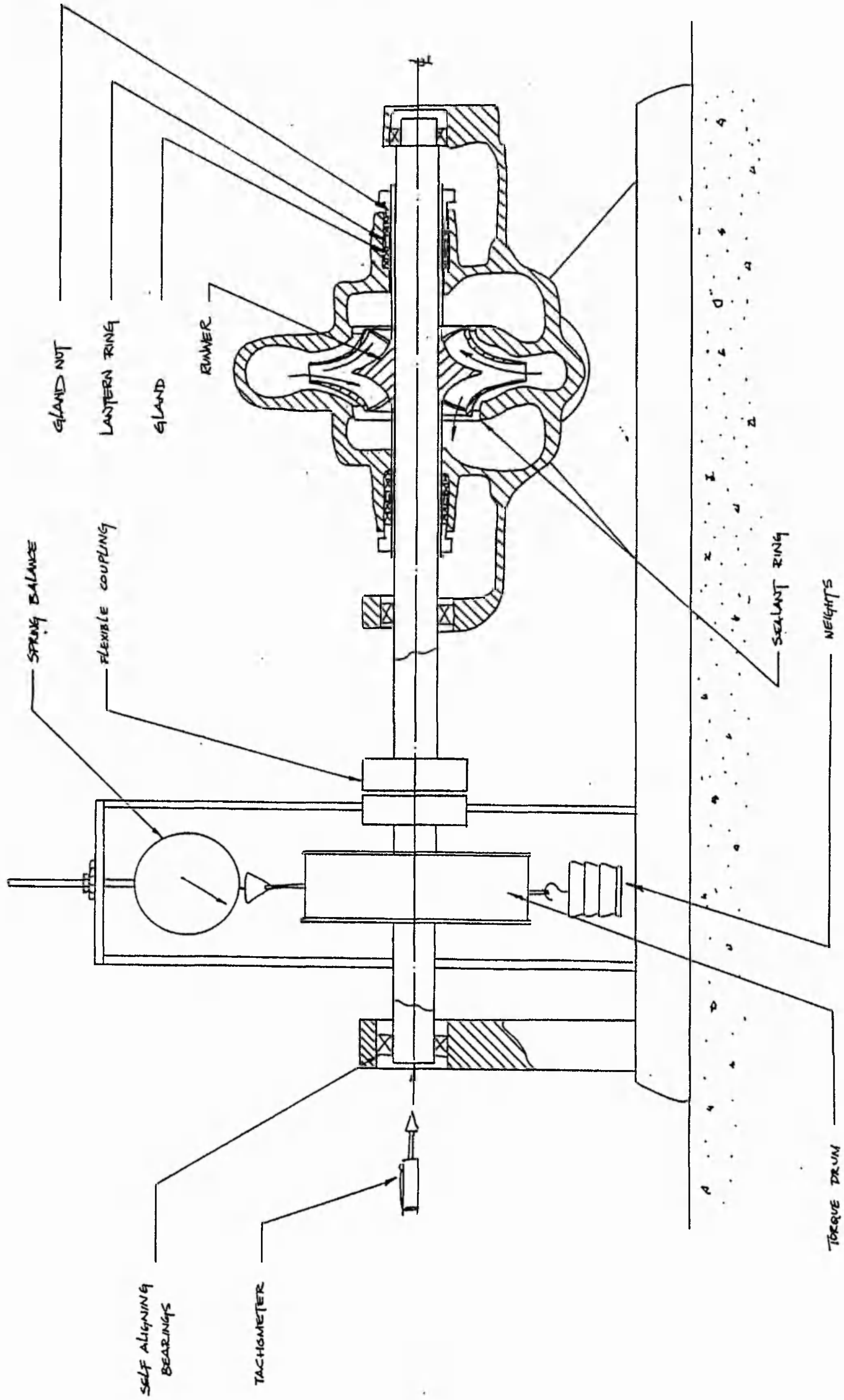


Figure H.2 Cross section of test rig

APPENDIX J

ALTERA EP300 PROGRAM

NETWORK:

```
CLOCK              = INP(CLOCK)
COMPA              = INP(COMPA)
COMPB              = INP(COMPB)
COMPC              = INP(COMPC)
COMPD              = INP(COMPD)
O0                 = INP(O0)
O16                = INP(O16)
O128               = INP(O128)
START              = INP(START)
SUSTRIP            = INP(SUSTRIP)
QA, QA             = RORF(DA, CLOCK, C, GND,VCC)
QB, QB             = RORF(DB, CLOCK, C, GND, VCC)
QC, QC             = RORF(DC, CLOCK, C, GND, VCC)
QD, QD             = RORF(DD, CLOCK, C, GND, VCC)
TRIP, TRIP         = COCF(TRIPc, VCC)
CLK                = CONF(CLKc, VCC)
TRIGGER, TRIGGER = COCF(TRIGGERc, VCC)
RESET              = CONF(RESETc, VCC)
```

EQUATIONS:

```
RESETc     = TRIGGER';

TRIGGERc   = COMPA' * COMPB'
           + COMPA * COMPB;

CLKc       = O0';

TRIPc      = (START * COMPD'
           + START * TRIP'
           + START * O0' * QB' * QC' * QD' * O128 * SUSTRIP * COMPB'
           + START * O32 * O0' * COMPA * QA * QB * QC * QD)';
```

```
DD      = QD * QA
        + QD * QB
        + QD * QC
        + COMPA * QD
        + COMPA * QA * QB * QC;

DC      = QC * QB * QA'
        + COMPA' * QC * QA
        + COMPA * QC * QB'
        + COMPA * QC * QD
        + COMPA * QC' * QB * QA
        + COMPA' * QC' * QB' * QD * QA';

DB      = COMPA' * QA * QB
        + COMPA * QA' * QB
        + COMPA * QA * QB'
        + COMPA' * QA' * QB' * QD
        + COMPA' * QA' * QB' * QC
        + COMPA * QA * QC * QD;

DA      = QA' * QD
        + QA' * QC
        + QA' * QB
        + COMPA * QA'
        + COMPA * QB * QC * QD;

C       = COMPC';
```

APPENDIX K

IGC MANUAL

Withheld by request of the collaborating establishment
to ensure that technology transfer is conducted
with an adequate level of quality control