

CRANFIELD INSTITUTE OF TECHNOLOGY

DEPARTMENT OF AIRCRAFT DESIGN

COLLEGE OF AERONAUTICS

Ph.D. THESIS

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**A study of the aeroelastic behaviour of helicopter
rotor blades featuring swept tips**

Supervisor

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SUMMARY

The design of a model helicopter rotor blade incorporating a swept tip is described. The swept tip is chosen to provide a coupling between the lift at the tip and the blade twist thus achieving a variation of blade twist both with azimuth and with forward speed. The design is the first step in an investigation of aeroelastic tailoring as a means of reducing helicopter vibration and increasing rotor performance.

The first prototype blade encountered stability problems and further designs were evaluated using a new modes/stability computer program developed within RAE. Comparisons are made between the stresses measured on a second stable swept tip blade and a dynamically similar rectangular blade. The results show that a beneficial twisting of the swept tip blade is achieved which enhances rotor performance and reduces the flatwise bending and torsional moments. Comparisons are made between the experimental results and those predicted by the RAE/WHL coupled modes analysis. The predictions are reasonably accurate for the flatwise moments but a more representative model of the hub is needed to produce acceptable predictions of the torsional moments.

The design of a further set of blades is discussed, the aim being to investigate the effects of introducing a strong coupling between the flap and torsion motions of the blade by sweeping back the shear centre. An analysis of the results shows that there are large gains in blade stability with no severe adverse effects on blade loads.

A theoretical investigation has been undertaken to observe the effects of tip sweep on the performance of a full size rotor. The results show that aft tip sweep can reduce control loads and rotor power for a rotor with a cambered aerofoil section.

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NOMENCLATURE

<u>Symbol</u>	<u>Dimensions</u>	<u>Description</u>
A	L^2	Area
A_0	(Degrees)	Collective pitch
A_1	(Degrees)	Lateral cyclic pitch
B_1	(Degrees)	Longitudinal cyclic pitch
C		Strain gauge calibration factor
C_{D_0}		Zero lift drag coefficient
C_{M_0}		Zero lift pitching moment coefficient
E	$ML^{-1}T^{-2}$	Young's modulus
EA	MLT^{-2}	$\iint_A E \, dn \, dy$
EIf	ML^3T^{-2}	Flat stiffness = $\iint_A E \, y^2 \, dn \, dy$
EIl	ML^3T^{-2}	Edge stiffness = $\iint_A E \, n^2 \, dn \, dy$
G	$ML^{-1}T^{-2}$	Shear modulus
G'	$T^2M^{-1}L^{-1}$	Ratio of strain gauge output to applied load
GA	MLT^{-2}	$\iint_A G \, dn \, dy$
GK	ML^3T^{-2}	Torsional rigidity
I		Second moment of area
K		Polar moment of inertia
$K_{m_1}^2$	L^2	(Flap radius of gyration) $^2 = \frac{1}{m} \iint_A \rho y^2 \, dn \, dy$
$K_{m_2}^2$	L^2	(Lag radius of gyration) $^2 = \frac{1}{m} \iint_A \rho n^2 \, dn \, dy$
L	L	Moment arm of applied load

Nomenclature cont.

<u>Symbol</u>	<u>Dimensions</u>	<u>Description</u>
m	ML^{-1}	Mass/unit length = $\iint_A \rho \, dn \, dy$
P	MLT^{-2}	Applied load
P_A	MLT^{-2}	Unknown applied load
r	L	Local spanwise coordinate (+ve root to tip)
R	L	Rotor radius
R_T	L	Spanwise extent of tip sweep
R'	V	Strain gauge voltage
V_a	V	Summed voltage across known resistors
V_G	V	Wind-tunnel test gauge voltages
V_t	V	Total voltage
V_1	V	Gauge supply voltage during bench calibration
V_2	V	Gauge supply voltage during wind tunnel test
X_n		Spanwise node number
X_{RF}	L	Distance along root fitting
X_s	L	Distance along shear centre locus
y	L	Local flapwise coordinate (+ve upwards)
Y_{CG}	L	Centre of gravity offset from shear centre = $\frac{1}{m} \iint_A \rho \, n \, dn \, dy$
Y_{EA}	L	Tension centre offset from shear centre = $\frac{1}{EA} \iint_A E \, n \, dn \, dy$
Z_{CG}	L	Vertical c.g. offset = $\frac{1}{m} \iint_A \rho y \, dn \, dy$
Z_{EA}	L	Vertical tension centre offset = $\frac{1}{m} \iint_A E n \, dn \, dy$
β	(Radians)	Pre-twist about r
η	L	Local chordwise coordinate (+ve T.E. to L.E.)
θ	(Degrees)	Geometric incidence

Nomenclature cont.

<u>Symbol</u>	<u>Dimensions</u>	<u>Description</u>
μ		Advance ratio
ρ	ML^{-3}	Density
ψ	(Degrees)	Azimuth angle
Ω	T^{-1}	Rotational speed
1F,2F...		Rigid flap mode, 2nd flap mode...
1L,2L...		Rigid lag mode, 2nd lag mode...
1T,2T...		Rigid torsion mode, 2nd torsion mode...

1 INTRODUCTION

The helicopter has been in the unique position for many years of being the only vehicle which can take-off from and land vertically onto unprepared surfaces and fly at modest forward speeds carrying a useful payload. The design of the helicopter has not changed significantly from the first successful vehicle to enter full production, the Sikorsky R-4, to the more modern Westland Sea King. Most of the changes have come from the increase in the power-to-weight ratio of engines; the introduction of jet turbines probably having the largest effect, and the use of lighter materials and structurally more efficient construction methods in the design of the fuselage and transmission. One item which has not changed significantly is the design of the rotor system including the aerodynamic design of the blades.

The role of the helicopter has been expanding during the last ten to twenty years. On the civil side, helicopters are widely used as air ambulances, observation platforms by the police, for the transportation of equipment and people to remote areas such as oil platforms and for crop spraying. On the military side, the roles are becoming more demanding. Helicopters are being used as advanced weapon platforms whilst having to survive an increasingly hostile environment. The complex systems found on helicopters which are required for survival on the modern battlefield, lead to the problems of reliability, maintainability and availability.

The helicopter design is still dependant on trying to reduce the empty weight of the vehicle especially the powerplant and the transmission, but two other factors are becoming increasingly important. The vibrational levels of helicopters are much higher than those of fixed wing aircraft throughout the flight envelope but especially when operating at high thrust or near maximum forward speed. The vibration leads to pilot fatigue and reduces the life of many helicopter components, including the weapon systems and avionics. Another disadvantage of the rotary winged vehicle is its inability to attain high forward speeds.

It is these two problems that are currently absorbing the largest proportion of resources for research into improving helicopter performance and are discussed in further detail.

1.1 Helicopter vibration

The lift produced by each blade as it rotates is nominally constant with time with the helicopter in hover. The loads at the hub have therefore minimal oscillatory components. The main possible source of vibration is from the interference between the rotor downwash and the fuselage, and this is usually only a problem if the distance between the plane of the rotor and the fuselage is small. In forward flight, the airspeed of the advancing blade is higher than for the retreating blade, which results in a changing spanwise lift distribution with azimuth. To maintain a balance in roll, the pitch of each blade is changed cyclically and although the rolling moment will be in balance, the aerodynamic loads will vary periodically. The interaction of the periodic aerodynamic loading and the dynamic response of the elastic rotor blade results in vibration at the hub. The vibration is then transmitted via the gearbox to the fuselage.

The alleviation of vibration has attracted much attention for many years with several different approaches being adopted. These include passive devices such as vibration absorbers. Some absorbers such as the Westland rotor head absorber[1] and blade pendulum absorbers[2] are tuned to particular rotor frequencies, but their effectiveness is reduced if the rotor speed changes. Bifilar absorbers[3] overcome this problem but require maintenance. The typical weight of such systems is less than 1% of the vehicle weight. Active systems such as higher harmonic control (HHC) [4-6] have been shown to be very effective. The principle behind these systems is to alter the lift produced by each blade at frequencies higher than the rotating frequency. This can be achieved by either changing the pitch of the blade or by altering the angle of trailing edge tabs. The benefit over the absorbers is that the amplitude of a large number of modes of vibration can be reduced compared with one or two modes for the absorbers. HHC is also self tuning and therefore adapts to changes

in rotor speed. A typical system[6] weighs about 1% of the total vehicle weight and consumes 3% of the installed power although the penalty is reduced if the same system is part of an automatic flight control system. The system can also be used to improve the aerodynamic performance. Another method of reducing vibration is by using active gearbox interface control (AGIC) [7] developed by Westland Helicopters Ltd (WHL). The gearbox is isolated from the fuselage by elastomer mounts incorporating an actuator. The weight of the AGIC system is similar to that of the HHC system, but it requires only a tenth of the power. The AGIC system has been demonstrated successfully on a Westland W30 helicopter.

The methods used to reduced vibration, mentioned above, all carry a weight penalty and a more beneficial solution may be to reduce the vibration at source through the design of blades that generate much lower levels of vibration, but this requires a better understanding of the influence of blade aerodynamic and elastic design on vibration. However, a clear demonstration of the fact that blade design can have a major impact on vibration has been provided by the flight tests of the British Experimental Rotor Programme (BERP) rotor blades by WHL[8]. The details of the BERP blade design can be found in section 1.2.

The vibration produced by current rotor blade designs is minimised by the careful placement of the blade natural frequencies away from the fundamental aerodynamic forcing frequencies[9]. The coupling of blade motions for example in the flap and torsion directions, can be minimised by positioning the chordwise shear centre and the centre of gravity near the aerodynamic centre. This also reduces the possibility of flutter. Rotor blades with either dynamic or aerodynamic coupling i.e. those designs which did not follow the above guide lines, were avoided in the past as the effects of these modifications were not fully understood and could have severe adverse effects on blade stability. With the introduction of more advanced computer prediction methods and more powerful computers, complex blade designs can now be undertaken.

A number of studies, both theoretical and experimental have addressed the problem of vibration reduction by changing the blade design[10-14], for example chordwise and spanwise mass distribution, chordwise shear centre position and planform shape. These studies have produced some encouraging results especially those involving the modification of the tip shape. Anhedral tips have been shown[15,16] to reduce one source of vibration by displacing the tip vortex below the rotor disc. The effect on blade lift of the vortex shed from one blade on the following blade is reduced. A theoretical study of a rotor blade incorporating a swept tip[17] has shown that tip sweep has a large influence on blade dynamic behaviour. There is the possibility, however, of modal frequency convergence which in some cases can lead to flutter.

1.2 Helicopter performance

The inability to attain high forward speeds is another disadvantage of a rotary winged vehicle compared to a conventional aircraft. This limitation is usually not due to the lack of installed power, but due to the aerodynamic limitations of the rotor blades. The pitch of the retreating blade increases with the forward speed of the helicopter as mentioned earlier. There is however a limit to this and it occurs when the blade starts to stall. Associated with the loss in lift is a sudden change in pitching moment. This condition therefore leads to large oscillatory loads in the control system and high blade stresses as well as the loss of roll control. An increase in rotor speed delays the onset of retreating blade stall but introduces adverse effects on the advancing side of the disc. The advancing blade tip experiences transonic flow at high forward speed leading to increases in blade drag, vibration and noise.

A number of alternative rotorcraft configurations have been investigated. The compound helicopter such as the Russian Mil-6, uses a small span wing to offload the rotor in forward flight. Vehicles such as the Rotor Systems Research Aircraft (RSRA) [18] have also turbofan engines to provide additional forward propulsion. The RSRA

vehicle has more recently been used to demonstrate the "X-wing" concept[19]. The rotor blades have been designed with high bending and torsional stiffnesses so as to be able to carry the weight of the vehicle with the rotor stationary. The aerofoil section is essentially composed of two leading edges and a trailing edge is produced by blowing air through one of two spanwise slots. The vehicle operates as a conventional helicopter up to about 200 kts. The rotor then slows down and the blades are used as wings to provide the lift whilst jet turbines are used for forward thrust. During transition, the side of the aerofoil that is used as a trailing edge depends on the direction of the local flow. The Sikorsky Advancing Blade Concept (ABC) [20] uses a contra-rotating coaxial rotor system. In high speed flight lift is produced by the forward and aft sectors of the rotor disc and by the advancing blade. Stability in roll is also maintained as there is an advancing blade on each side of the disc. The contra-rotating rotor system also does away with the need for a tail rotor. Finally, the most recent type of rotorcraft to have flown is the Bell-Boeing V-22 Osprey tilt rotor vehicle[21]. A large propeller/rotor is mounted at the outboard end of each wing and is allowed to tilt so as to provide vertical or forward thrust. At low speeds the rotors are in the horizontal plane to provide vertical thrust and control. In high speed forward flight the rotors are tilted 90° forward so that they work as propellers and the wings provide the vertical lift.

A large increase in the forward speed capability of the rotorcraft can be achieved by all of the vehicles mentioned above but at the expense of a reduced useful load fraction compared to a conventional helicopter. The economic viability of these vehicles rests on the type of missions that are flown. Helicopter designers are seeking improvements therefore by considering blades with new aerofoils and complicated tip shapes and with the introduction of composite materials complex blade shapes can be built.

In forward flight the forward and aft sectors of the helicopter rotor disc produce most of the lift[22]. Cambered aerofoils are being used to increase lift and the advanced aerofoil sections can vary along the rotor blade. The Westland Lynx has, for example, a cambered

RAE 9615 aerofoil at the root which blends into the thinner RAE 9617 at the tip. The thinner aerofoil reduces the Mach number effects experienced at the tip of the advancing blade. Simple modifications to the tip shape of other rotors have also been used to reduce the onset of shock waves[23-25]. One such exercise involved the wind tunnel testing of a rigid blade[26] followed by full scale flight trials on a Westland/ Aerospatiale Puma helicopter[27]. The tests showed significant reductions in power required. The lessons learnt from the exercises were used in the BERP blade design and a measure of the success is the recent world speed record attained by the Westland Lynx[28] fitted with BERP3 blades. These blades use advanced aerofoil sections and a swept tip which reduces Mach number effects on the advancing side of the rotor disc and also generates strong vortices to delay stall on the retreating side. The blade has three aerofoil sections along its span; an RAE 9634 section in the tip region - a thin aerofoil which has lower surface velocities compared with thicker sections for a given incidence and at high Mach number, the RAE 9645 section just inboard of the tip - a high lift section, and an RAE 9648 reflex section at the blade root. The reflex section is used as it has a nose-up pitching moment which balances the high nose-down pitching moment of the RAE 9645 aerofoil.

The choice of the blade parameters such as the twist is a compromise between hover performance and high speed forward flight. High twist, which is beneficial in hover, is a disadvantage at high speed since the blade tip on the advancing side of the disc operates at a negative incidence. The inboard portion of the blade however is at a positive incidence which leads to high flapwise stresses in addition to any adverse effects arising from transonic flow phenomena. The amount of twist therefore needs to be reduced on the advancing side of the disc as the forward speed of the helicopter increases whilst retaining high twist on the retreating side. An analysis on the effects of twist on helicopter performance[29], suggested that live twist could reduce the cruise penalties of high twist rotor blades.

An azimuthal variation of blade twist can be achieved by introducing offsets between the aerodynamic centre, the torsion axis and the section centre of gravity or by having a structural coupling between two blade motions such as flap and torsion. The so called aeroelastically adaptive rotors are capable of redistributing the radial and azimuthal lift not only to improve the aerodynamic performance, but also to reduce vibratory loads[30].

1.3 Blade loads prediction programs

The helicopter can be split into a number of distinct components for the purpose of predicting the aerodynamic and dynamic performance: the main rotor, the fuselage and the tail rotor. Each part is exposed to an airload and responds also to a periodic forcing whether it is aerodynamic or a dynamic forcing from an adjacent structural component. The performance of each of these items can be predicted with varying degrees of accuracy depending on the assumptions made, the technique used to solve the problem, and the level of empiricism. The performance of one of these sections also has a significant effect on the performance of an adjacent part. For example the wake from the main rotor affects the airflow around the fuselage and tail rotor, and hence alters the trim of the vehicle.

The prediction programs used in todays rotor blade designs can vary in complexity from the simple performance of a isolated rigid rotor, to the calculation of the loads on the rotor of a complete model in a manoeuvre such as the C-81 program developed by Bell Helicopter Textron[31]. The more complex programs require longer computing times than the simpler methods although, with the increasing availability of high speed computers, it is now possible to use fairly elaborate programs for parametric studies whilst more complex analyses such as those used in the prediction of the 3-D compressible flow around a complex blade tip[32], are currently used mainly in the support of wind tunnel tests.

Much interest has been placed in the application of optimisation methods in the helicopter design during the last few years. Although methods have been available for the design of fixed wing aircraft, the use of these methods in the helicopter field has been delayed owing to the lack of reliable analytical prediction methods and the complexity of the helicopter design problem. A review of the application of numerical optimisation methods for helicopter design[33] divides the problem into two parts: the global optimisation which looks at the overall configuration to meet mission requirements and the local optimisation where only the prime components are addressed. A number of analyses have been undertaken[34,35] which have considered the dynamic aspect of blade design. The aim has been to reduce oscillatory loads by altering the blade structural properties whilst maintaining aeroelastic stability. The methods are based on finite element analyses and results show large reductions in the 4/rev hub loads for a four bladed rotor. Celi and Friedmann[36] introduce also tip sweep into their analysis and conclude that it can lead to further reductions in blade vibration levels. Another analysis[37] addresses the aerodynamic optimisation of the rotor. The variables considered are the aerofoil section, blade chord taper, chord and twist, and the aim is to minimise hover power whilst assuring a good forward flight performance. The results show that the methods are as good as using the parametric study approach. The optimisation method is however ten times faster than the parametric study although there is no information on how, for example, the rotor performance varies with changes in the design parameters.

Optimisation techniques have been used separately for dynamic and aerodynamic aspects of rotor design, but there are few methods available which address both areas simultaneously. Further work is required in the development and validation of such methods and available computing power is still an important limitation.

The prediction programs used in the present analysis are the modes calculation and rotor loads analysis developed by the RAE and WHL[38]. The technique which is described in more detail in Appendix A uses a modal approach. The advantage of this method over a finite

element approach is that the computation times are much shorter making the programs more suitable for parametric studies. The method produces also the forcing from each mode and this helps to provide a better understanding of the behaviour of the rotor.

1.4 Scope of work

Aeroelastically adaptive rotors are not easy to investigate because of the large number of possible parameter variations. There are also a number of ways by which aeroelastic tailoring can be achieved. These include an oscillating flap which requires a secondary control system, the use of non-symmetric layers of fibres in the blade construction to produce a coupling between blade motions, or tip sweep to introduce a coupling between the lift at the tip and the torsion response of the blade. The first option is not possible with the current rotor test rig used in the RAE 24ft wind tunnel and the second technique requires computer programs in the design stage which can make use of cross-stiffnesses in the prediction of blade loads. These programs are currently under development. A simple swept tip with the aerodynamic centre aft of the flexural axis of the inboard section is therefore considered as the first step.

The overall aim of the present study is to develop an understanding of the dynamic response of a rotor with a swept tip designed deliberately to introduce some of the couplings mentioned above which were avoided in the BERP design. The full aerodynamic benefits of the tip cannot be investigated because of the limited forward speed capability of the RAE 24 ft wind tunnel in which the rotor is tested. To achieve the correct advance ratio the rotor speed is reduced and therefore there are negligible compressibility effects at the blade tip on the advancing side of the rotor disc. The study demonstrates however the possible benefits as well as showing the problems associated with designing and flying a swept tip blade.

The study is divided into a number of phases. The first phase, which is described in section 2, addresses the design of a model rotor blade incorporating a swept tip. The main aim of this particular

exercise is to validate the design tools and prediction methods. The design is based on an existing rectangular blade which has previously been flown in the RAE 24ft wind tunnel. The rectangular blade is used later as a baseline blade with which comparisons are made between the loads on this blade and on the new swept tip blade. The main guideline for the design of the swept tip blade is that it has to have similar modal frequencies to the baseline blade so that valid comparisons between these blades can be made. The blade and hub moments must also be within the same limits as for the design of the baseline blade. This minimises the detail design required for the new blade. Finally, the blade has to be stable. A preliminary study is made to evaluate the spanwise extent of the tip and this is followed by a refinement of the design to meet the above mentioned requirements. The blade design tools are discussed in more detail in Appendix A, and a description is given of the way in which the whole rotor has been modelled.

The final Mk1 design is tested on a hover site prior to installation in the wind tunnel. The blade is found to be unstable just below its normal operating speed, despite the fact that a standard stability program showed that it should be stable. At this point a new stability analysis is made available and it predicts well the nature of the instability. Section 3 discusses the design of a new blade, Mk2, using this new stability analysis in parallel to the other design tools. The stability analysis is complemented by a series of experiments which are considered necessary due to the unproven nature of the analysis. A stable blade design is found by increasing the torsional stiffness and introducing some additional chordwise mass balancing. Following a successful wind tunnel test of this Mk2 blade comparisons are made between the stresses on the blade and on the baseline blade. This is followed by comparisons with the stresses predicted using the blade design tools. Results are presented for a number of thrusts and advance ratios. A further modes prediction program, which includes a better representation of the hub, is used also to assess if an improvement can be made in the prediction of the blade torsional moments.

Once confidence in the analytical methods is established, the second phase involves the design and testing of blades incorporating further aspects of aeroelastic tailoring. The aim is to explore the possible benefits of introducing a large flap/torsion inertia coupling by sweeping back the shear centre relative to the centre of gravity. One possible benefit for this type of blade is an increase in the flutter speed. The design and testing of the Mk3 blade is discussed in section 4. Comparisons are then made with the Mk2 swept tip blade and with the predicted results.

The prediction programs are used to look at the effects of introducing aeroelastic tailoring on blade stability and on the vibration and performance of a modern helicopter in the final part, section 5. The vehicle chosen is a Puma helicopter which has a four bladed articulated hub. The blade structure is modified in a similar way to the model rotor and comparisons are made between the original blade and the modified blade to assess whether aeroelastic tailoring using swept tips is a practical proposition at full scale.

An abridged version of sections 2 and 3 was presented at the 14th European Rotorcraft Forum, Milan, Italy, in 1988. The same paper was accepted for publication in Vertica and is included in this thesis as Appendix B.

2 DESIGN OF THE MK1 BLADE

2.1 The principal blade parameters

A rotor test rig was designed and built at the RAE[39] just over a decade ago for testing in the RAE 24ft wind tunnel, which is a low speed test facility. The rig was modernised following some initial tests, to include a new dual load path hub[40] and composite blades with a cambered aerofoil section. A number of experiments were undertaken[41,42] on two designs of rectangular blade[43,44]. The first design had a high torsional stiffness which gave a 1st torsion frequency/rotational frequency ratio much higher than that found on current full-size blade designs. The second design had a lower torsional stiffness giving a 1st torsion frequency of 4.62. This was achieved by using an improved manufacturing technique. Both blades had an RAE 9642 aerofoil section[45], a chord of 0.14m and a 4.4 deg/m twist.

The rectangular blade with the lower torsional stiffness was chosen as the baseline blade for the swept tip blade programme. Extensive wind tunnel test data were available for this blade, and a blade mould with a 20° swept untwisted tip was also available with the same aerofoil section (Table 1) and chord as for the rectangular blade. The design of the first swept tip blade was limited therefore in the possible variations of blade geometry, Fig 1. The rotor radius measured from the centre of rotation to the tip taking into account the 2° pre-sweep and the tip sweep is 1.8 m. This is the same as the baseline rectangular blade. The pre-sweep, which is used to position the blade in lag so that the centrifugal load and the blade drag cancel each other out, was assumed to be 2° as for the baseline blade, although it could be altered if the steady lag loads are too high. The blade root is set at an angle to the horizontal in the flap direction hence, the flap mean loads cancel the centrifugal load. This precone angle is set at 5° although, if necessary, it could be altered by changing the precone blocks on the hub (section 2.2.1).

The remaining design variables are the blade structural properties and the spanwise extent of tip sweep R_T having fixed most of the blade geometry. The evaluation of various tip sizes is presented in section 2.3. As a starting point in the design the swept tip blade was assumed to have the same form of construction and properties as the baseline blade. The main section of blade, Fig 2, is the same as the baseline rectangular blade and has the structural properties listed in Table 2. The tip is similar in construction to the inboard section except that the torsional stiffness was increased to reduce the possibility of an instability. This was achieved by adding a Kevlar fairing utilising a symmetric layup of fibres at ± 30 deg to the spanwise axis. The leading edge weight was removed as the cg of this additional mass was aft of the blade shear centre and would hinder attempts to mass balance the blade. As the blade construction is the same as for the baseline blade, the maximum bending moments and torque are applicable. These limits are based on static and fatigue tests made on specimens of the rectangular blade, the critical part being the attachment between the blade and its root fitting. The maximum blade bending moments, reduced by a factor of at least 1.5 from the ultimate bending moments, are 40 Nm in the flatwise direction, 100 Nm in the edgewise direction and 20 Nm in torsion. The limiting bending moment for the swept link (a full description of the dual load path hub can be found in section 2.2.1) is a linear combination of the flatwise and edgewise bending moments. The maximum bending moment in the flatwise sense is 43 Nm, and in the edgewise direction it is 82 Nm. The limiting loads for the pitch link in tension, flap flexure flap force and flap flexure lag force are 120 N, 175 N, and 300 N respectively.

2.2 Theoretical model of the rotor

The analytical methods that were used in the design of the swept tip blade are discussed in Appendix A. The modelling of the dual load path (DLP) hub and the model blade is described in the following two sub-sections.

2.2.1 The hub model

The DLP hub shown in Fig 3 is not a model of a hub used by a full size helicopter. It was designed within the RAE to look at the benefit of hingeless / DLP rotors with the intention that these could be used in future full-size designs. Some hingeless rotors such as those on the Westland Lynx and MBB BO-105, have bearings in the pitch change mechanism. The DLP hub has an elastomeric bearing which has the advantages that it requires no lubrication or maintenance.

The DLP hub, as the name suggests, is divided into two load paths. Fig 4 shows the load path which carries the blade centrifugal load. The blade root is attached via an interchangeable swept link, which can have various degrees of sweep and post-cone, to the tension member. The tension member is not attached to the coupling bearing (Fig 3) and therefore the blade torque is transferred directly to the pitch link and no centrifugal load passes to the coupling bearing. The yoke is attached to the support pillar via an elastomeric bearing which permits movement in all directions although it is fairly stiff in translation. The root end of the yoke is attached to the pitch link via the pitch horn. Fig 5 shows the flap and lag flexure elements. Blade flap and lag loads are transmitted via the tension member to the coupling bearing. The flap loads are passed to the support frame and then onto the flap flexures, whilst the lag loads are reacted by the lag elastomers. The elastomer is needed to provide damping in the lag direction so as to avoid ground resonance. Residual lag loads are passed to the flap flexure. The root of the flap flexure is attached to the spool via interchangeable tapered blocks which allow a precone angle to be set.

A number of idealisations had to be made in the modelling of the DLP for the coupled modes program (J134). The two load paths are replaced by a single load path carrying the centrifugal load with the flap and lag loads reacted by springs to earth, Fig 6. The flap and lag springs have equivalent stiffnesses to the flap flexure in the flap direction and the lag elastomer respectively. The tension member is attached at its root to earth via a low stiffness element which has

the structural properties of the elastomeric bearing. The model of the hub assumes that the stiffness of the lag elastomer does not change with frequency. Young and Thompson[42] showed that the stiffness of this elastomer is different at the 1L (first lag or rigid lag) mode frequency compared with the 2L mode frequency. No account is taken of the fact that the inertia of the frame surrounding the coupling bearing is present only in flap and not in lag and the rotational inertia about the flap axis for the coupling bearing and frame is not represented.

The rotor is trimmed during the wind tunnel experiment by minimising the 1 Ω flapping signal on the flap flexure. From Fig 3 it can be seen that the flap flexure load in flap is affected by the centrifugal force on the coupling bearing and frame. This effect has to be taken into account since it is important that the rotor is trimmed in the same way in the prediction programs as in the experiment.

Table 3 lists the spanwise position of the blade and hub components and Table 4 contains the structural properties. The flap and lag springs have stiffnesses of $3.78 \times 10^4 \text{ Nm}^2$ and $7.3 \times 10^4 \text{ Nm}^2$ respectively and the pitch link / control system has a stiffness of $4.7 \times 10^5 \text{ Nm}^2$.

2.2.2 The blade model

The global coordinates for a uniform rectangular blade are fairly easy to calculate. The rotor is divided into a number of sections where each node represents the root or tip of the rotor, a change in direction of the shear centre or the attachment of a load path to earth. For the baseline blade the shear centre is coincident with the quarter chord and blade twist will therefore have no effect on the global coordinates of the shear centre. The first node is the hub root, the second represents the point at which the pitch link loads enter the hub, the third is the point at which the cone and sweep start and the final node is the blade tip - Fig 7. The spanwise position of the blade components and the structural properties are

also found in Tables 3 and 4 respectively with a full definition of the properties in the nomenclature. For the junction between the main section of blade and the swept tip, the shear centre and the other structural properties are linearly interpolated over 36mm.

It is important to explain at this point the directions in which the blade bending moments are both measured and predicted. The coordinate systems are shown in Fig 8. The first comprises the global coordinates 'X', 'Y' and 'Z'. This system rotates with the blade at an angular velocity Ω . The motions of a point on the blade in the directions 'Y' and 'Z' are referred to lag and flap motions respectively. These are independent of the blade pitch angle θ . The local coordinates 'r', ' η ' and 'y' are measured radially, chordwise and perpendicular to the chord respectively. The 'y' and ' η ' directions are known as flatwise and edgewise directions respectively and are positive in the directions shown. A 'nose up' pitching of the blade will also be positive. The moments measured and predicted are in the local coordinate system.

2.3 The tip size

The first stage in the design of the swept tip blade was to determine the spanwise extent of the tip, R_T , Fig 1. The tip had to provide sufficient torque to twist the blade but the bending moments and loads could not exceed those noted in section 2.1.

A range of blade designs was considered with the tip size varying between 8% and 20% radius but corresponding changes to the inboard section of the blade were necessary to keep the designs as similar as possible. The overall centre of gravity was positioned at the quarter chord of the main blade section by removing the trailing-edge weight and adding mass to the leading-edge. For the blade with a 20% R tip the mass, 1.07 Kg, was substantially higher than for the rectangular blade, the additional mass being dependent on the size of the swept tip. The flap and lag inertias were kept appreciably constant for each tip size by adding mass at the quarter chord to give a total mass of 1.07Kg.

The swept tip is introduced into the J134 modes program by sweeping back the shear centre locus by 20° at the relevant radial position. The spanwise variations of the structural properties are the same as for the baseline blade except for the blade tip as mentioned earlier. The blade inertias were recalculated for each case where the mass had changed, although in reality, the addition or removal of lead ballast affects the other structural properties such as the flatwise and edgewise stiffnesses. The latter changes are however small and are not included therefore.

The unsteady aerodynamic terms were retained in the performance program and the vortex ring downwash model was used with the interactive near wake. The rotor was trimmed to eliminate the 1/rev force in the flap load path to earth. The calculations presented in this section are all made at an advance ratio of 0.34 and at a 900N rotor thrust. The rotor thrust coefficient/solidity is 0.075 at the normal operating speed of 600 rev/min.

The loads on the blade depend on the proximity of the modal frequencies to multiples of the forcing frequency thus the effect of the tip size on the frequencies is important. The variation of the frequencies of the first eight modes with tip size is shown in Fig 9. The first lag and the first flap modes, indicated by 1L and 1F in Fig 9, show a negligible variation in frequency with tip size since the characteristics of these modes are dominated by the stiffnesses of the elastomeric dampers and the flap flexure at the blade root. The fourth and fifth modes are highly coupled third flap and first torsion modes but the appearance of the modes and the scaling used make it difficult to determine which component is dominant. The fourth and fifth modes also show a large variation in frequency with tip size. The variation occurs because the tip is mass balanced by increasing the leading-edge weight. The local centre of gravity of each part of the blade is offset from the local shear centre resulting in a large pitch-flap coupling in addition to the undesirable increase in the torsional inertia of the straight part of the blade. A better approach would have been to add a concentrated mass at the leading-edge of the blade/tip junction but a numerical problem with the modes calculation

prevented the analysis of such a configuration. Some of the modal frequencies shown in Fig 9 are close to multiples of the forcing frequency. Care is necessary therefore when looking at the blade loads, as dynamic amplification can distort the trends with tip size. These resonances can be avoided by a slight alteration to the blade structural properties if required.

The change in magnitude of the calculated blade stresses across the span with tip size is shown in Fig 10 for the flatwise bending moment, Fig 11 for the edgewise moment and Fig 12 for the torsional moment. The corresponding hub loads on the swept link, the flap flexure and the pitch link are shown in Fig 13. The stresses on the blade are within the allowed limits for all sizes of tip. The pitch link limit load is exceeded however by the blade with a 18%R tip and both the combined stress on the swept link, and the pitch link load are greater than the allowed maxima for the largest size of tip considered.

The effect of the tip size on the elastic blade twist is shown in Fig 14. The figure shows the difference between the twist calculated at 270 deg azimuth, the retreating side of the rotor disc, with that at 90 deg azimuth, the advancing side, where twist is defined as the difference between the torsional displacement at the root cut out and the displacement at the tip. For the baseline blade $[\Delta\theta_{270} - \Delta\theta_{90}]$ is about -4.5° and $[\Delta\theta_{270} - \Delta\theta_{90}]$ increases with tip size. The tip sizes considered clearly give a beneficial effect compared with the rectangular blade in that the twist is increased on the retreating side ($\Delta\theta_{270}$) of the disc relative to the advancing side ($\Delta\theta_{90}$).

The results of the calculations presented in Figs 10 to 13 show that a swept tip extending over 16% of the blade radius is possible but other considerations need to be taken into account. As mentioned in section 1.1 one of the guidelines in the design of a stable blade is to locate the overall centre of gravity of the blade at the quarter chord. A large weight is therefore needed near the leading-edge of the straight part of the blade. The balance weight increases the total

blade mass by 15% for the 16% span swept tip and the mass increase is however only 5% for a 12%R tip. The smaller tip gives a 4 deg increase in the twist between the retreating and advancing side of the disc relative to the rectangular blade, Fig 14, which was considered adequate for a first design. The tip extending over 12% of the blade span was selected therefore for a more detailed investigation into the construction of the blade.

2.4 The final design

The blade with a swept tip over the outer 12% of the radius provides an adequate once-per-rev twisting of the blade and acceptable loads. Part of phase one of the study is however a comparison between the swept tip blade and the straight baseline blade, thus some refinement to the swept tip blade design was necessary to provide as close a match as possible of the modal frequencies and structural properties to those of the baseline blade.

Various mass distributions were possible to bring the overall centre of gravity of the blade near to the quarter chord of the straight portion. One solution was to use a lumped mass near the tip junction but that had the disadvantage of introducing sudden changes in the blade properties which are sometimes difficult to represent adequately in the analysis. A distributed mass is preferred and one design simply had the original trailing-edge weight removed and mass added at the quarter chord to increase the weight to that of the baseline blade. The blade bending and torsional moments for that design were within the limits and the blade was theoretically free from flutter[46] even though the blade was not fully mass balanced. That design was chosen for the Mk1 swept tip blade.

The overall centre of gravity of the blade is at 28.3% chord with the centre of gravity of the straight section at 23.2% chord. The other differences between the baseline blade and the chosen design for the Mk 1 swept tip blade, whose properties are listed in Table 2, are a 5% increase in the mass per unit length and reductions of 5% in the flatwise and 22% in the edgewise radii of gyration.

The increase of mass aft of the flexural axis of the straight portion of the blade due to the swept tip increased the torsional inertia of the blade and reduced the torsional frequency. Ideally the modal frequencies of the baseline and swept tip blade should be as similar as possible to allow valid comparisons to be made. The calculated reduction in the torsional frequency was small however and the difference between the two blades was acceptably small so that no change in torsional stiffness was necessary.

Fig 15 shows the spoke diagram for the swept tip blade. The frequencies were calculated without allowance for any structural or aerodynamic damping. Fig 16 shows the blade mode shapes calculated at the normal operating speed. The modes are scaled to a unit tip displacement rather than to a unit generalised mass. Corresponding plots for the baseline blade are shown in Figs 17 and 18. The most noticeable differences in the spoke diagrams are the relative positions of the third flap and the first torsion modes although, as noted previously, there are problems in resolving the nature of the fourth and fifth modes for the swept tip blade. Comparison of the mode shapes shows also that the swept tip blade has larger couplings between flap and torsion in the higher order modes.

The rotor blades were manufactured as specified. The instrumentation on the rotor and the results of some initial tests are described in the next two sections.

2.5 Rotor instrumentation

The three bladed rotor has strain gauges on one blade measuring flatwise, edgewise and torsional stresses at ten spanwise positions, Fig 19. The gauges at stations 4 and 6, 52.5% and 67.5% radius were omitted due to the limited number of slip rings available to carry signals from the rotor to the data acquisition system. The remaining blades have only one strain gauge position at 30% radius, station 1. Measurements were made also on the swept link, flap flexure and pitch links on two blades. A full description of the strain gauge installation and calibration can be found in Appendix C.

2.6 Hover tests

Tests were made on a hover site prior to the installation of the model in the wind tunnel. The main purpose of the test was to spin the rotor to 20% above its normal operating speed to check the integrity of the system and to identify other problems that could occur. One such problem became apparent when the rotor fitted with the Mk1 swept tip was spun up, for as the rotor approached its normal operating speed the trailing-edge section over the inboard part on all the blades separated from the spar. No damage was done as the debris was contained by the chain link safety curtain surrounding the rig.

Close inspection of the blades showed that the trailing-edge had detached near the root of the blade suggesting that a stress concentration in that area had caused a crack to propagate along the fibres. The stress at the root was probably increased by the forward lag loads on the blades caused by the pre-sweep. The pre-sweep is intended to provide a balance between the centrifugal loads on the blade and the steady aerodynamic drag force. The setting of 2 deg is ideal for the straight blade but about twice that required for the swept tip blade. The swept link was however not redesigned as the edgewise bending moments were not excessive. An alternative explanation for the separation of the trailing-edges was that the blades were dynamically unstable.

The spars of the blades and the strain gauges were not damaged in the accident so the blades were rebuilt for further investigation. A carbon fibre strap over the blade root was included in the rebuild to eliminate the stress concentration. The rotor speed was slowly increased and data were taken at regular intervals to monitor the loads. The rotor went unstable at 576 rev/min but the loads, Fig 20, did not reveal any significant increase in the oscillatory components before the critical speed was attained. A video film taken during the test showed that the tip motion contained large torsional displacements hence the instability was due to flutter rather than ground resonance. Further investigations were made therefore to

determine the cause of the flutter and to identify the modifications required to manufacture a flightworthy blade. These are discussed in the following following.

3 DESIGN AND TESTING OF THE MK2 BLADE

3.1 Blade stability

The stability analysis used in the design of the Mk1 swept tip rotor blade[46] had been validated only for tail rotors with rectangular blades. The incorporation of a swept tip into the blade design could have introduced errors in the stability analysis mentioned in section 2.4. Fortunately a new modes calculation and stability analysis[47] became available in time for investigating the swept tip blade. The results from calculations using this analysis are presented in this section. Also, the experimental evaluation of remedial measures, which has been described elsewhere[48], is summarised here for the sake of completeness.

The new stability analysis, developed at the RAE, calculates the blade mode shapes with applied aerodynamic loads. The modes are complex because aerodynamic, and optionally, structural dampings can be included. The programme then calculates the damping for each of the modes which is expressed as a percentage of the critical damping. The input data to this programme is fairly similar to that required for the modes programme J134 described earlier. The main difference is that there is no separate performance calculation and a simplified representation of the aerodynamics of the blade is included.

Calculations for the Mk1 swept tip blade indicated an instability at 574 rev/min compared with the experimentally obtained value of 576 rev/min. The theoretical results showed also that the problem was due to the convergence of the third and fourth modes. The third mode was the second flap mode and the fourth was a highly coupled flap/torsion mode. The calculated percentage critical damping plotted against rotor speed for the two modes is shown in Fig 21. A spoke diagram of the predicted damped modal frequencies showing the two modes converging is presented in Fig 22. The good prediction of the flutter speed gave some confidence in the new stability prediction

method, although, experiments were still thought necessary in view of the unproven nature of the analysis.

The standard method for alleviating flutter for fixed wing aircraft is to increase the torsional stiffness of the wing or to introduce extra chordwise mass balancing. The problem with the aircraft wing is slightly different from the rotor blade instability in that wing flutter usually involves the fundamental flap and torsion modes. The next step is to use the stability programme to investigate the effect of increased torsional stiffness and additional mass balancing on the rotor stability.

The predicted effect on the modal frequencies of adding an 80 gramme mass to the leading-edge of the blade is shown in Fig 23. The mass was added onto a 10mm portion of the main section of blade at the blade/tip junction. The mass, centre of gravity and inertia terms in the input data were updated although the increase in stiffness was considered negligible. Figure 23 shows only the third, fourth and fifth modes as the fundamental lag and flap modes were unchanged. The corresponding modal dampings for the third and fourth modes are shown in Fig 24. The addition of the mass, which represents 10% of the total blade mass, increases the flutter speed by 30% and reduces the rate at which the damping decreases with increasing rotor speed. A 10% increase in torsional stiffness, Figs 25 and 26, is less effective than an increase in leading-edge weight. The flutter speed increases by only 6% and the rate of change of damping is virtually unchanged. The addition of mass to the blade is therefore the better approach to solving the flutter problem. The results show also that mass balancing and increasing the torsional stiffness are acceptable ways of improving blade stability when non-fundamental modes converge.

To determine the optimal spanwise location of the mass further calculations were made. The results show that moving the mass away from the tip junction nearer the blade root reduces its effectiveness, presumably because it is nearer to the nodes of the second and third flap modes. Moving the mass outboard on to the swept tip reduces the

chordwise mass balancing and makes it more difficult to decrease the amount of coupling between the third and fourth modes.

Experiments were undertaken to complement the theoretical analysis mentioned above. The tests[48] were made on a different rotor rig to that used in the wind tunnel so there were some variations in both the aerodynamics and dynamics of the two facilities. The rig was equipped with the dual load path hub although only one blade was flown and counterbalance weights were installed in place of the remaining blades. The rig did not have a control system so the blade could only be flown at a fixed pitch. The rotor was surrounded by a safety net which had a smaller diameter and lower porosity than the net used at the wind tunnel hover site. An exciter was mounted on top of the hub to shake the blade either in the flap, lag or torsion using a pseudo-random frequency sweep. The frequency and damping of each mode was determined by analysing the output from one of the blade strain gauges. This was repeated for various rotational speeds up to the point when the blade fluttered.

The Mk1 swept tip blade was installed on the rig and the rotational speed was increased slowly until the blade went unstable. The blade fluttered at 610 rev/min compared to 576 rev/min on the wind tunnel hover rig. The stiffness of the test tower in addition to the aerodynamic and dynamic differences mentioned above, could be responsible for the higher flutter speed. At the time of writing further tests to explore this discrepancy were planned by the Dynamics Section of the Helicopters Division, RAE Farnborough.

A series of tests was also made on blades with the torsional stiffness increased relative to the Mk1 blade and additional mass added to a 0.1 m length of the leading-edge. For these tests the Mk1 swept tip blades were used. The torsional stiffnesses tested were 56 (the original stiffness of the Mk1 blade), 68, and 75 Nm² with masses of 40, 80, 120 and 160 grammes. The stiffness was increased by adding a narrow strip of carbon fibre on the top and lower surfaces of the blade. The fibres were orientated at +/- 45° in a symmetric lay-up

to avoid a structural flap/torsion coupling and to minimise the increase in flatwise stiffness. The additional mass was made from sheet lead and was glued around the leading edge at the blade/tip junction.

The flutter speed corresponding to the various combinations of mass and torsional stiffness is plotted against the leading-edge mass in Fig 27. The straight lines drawn on the graph show the trend of the results. The arrows on two of the points indicate that the test was terminated due to large oscillatory motions of the tower and not due to blade instability. The higher and lower points show the results from tests made on blades with torsional stiffnesses of 75 Nm^2 and 68 Nm^2 respectively.

Three bounds can be placed on the combinations of torsional stiffness and mass when selecting a suitable design. The absolute minimum flutter speed required is 680 rev/min on the laboratory tower. This is assuming that there is a 30 rev/min reduction in speed when moving to the wind tunnel rig and accepting a relaxation of the overspeed criterion from 720 to 650 rev/min. The value of 720 rev/min, which is equivalent to an overspeed of 20%, was the accepted guideline used originally during propeller tests in the RAE 24ft wind tunnel. The guideline was reduced for the rotor as it was demonstrated that the fatigue life was much larger than the anticipated total flying time of the blades. The fatigue test was also made at grossly elevated loads. The second bound limits the additional mass to no more than 80 grammes since greater masses produce unacceptable torsional loads at the tip junction. Finally, torsional stiffnesses greater than 75 Nm^2 increase the fundamental torsion frequency too much to enable a valid comparison to be made with the straight baseline blade.

The compromise selected for the Mk2 blade was a torsional stiffness of 68 Nm^2 and a leading-edge mass of 80 grammes. This gives a flutter speed of about 700 rev/min for a blade mounted on the dynamics test rig, or 670 rev/min for the wind tunnel rig.

A new set of blades was built with the higher torsional stiffness and additional leading edge weight. The structural properties of the new Mk2 swept tip blade are listed in Table 5 and include the properties for the section of blade with the increased leading edge weight (82-88%R). Details of the method of construction are provided in the Appendix D. A spoke diagram of the undamped modes, Fig 28, compared with Fig 15, for the Mk1 blade, shows that the frequency of the coupled third flap/torsion mode had decreased and the frequency of the fourth flap mode had increased. The mode shapes, Fig 29, had altered however only slightly when compared with the previous blade, Fig 16.

The blades were first tested on the wind tunnel tower positioned on the hover site. The flutter speed was 661 rev/min compared with the expected speed of about 670 rev/min when extrapolating from the laboratory stand results. The difference may be due to the position of the additional balance weight which was installed behind the existing leading-edge weight instead of being attached to the blade surface. A flutter speed of 661 rev/min is acceptable, but below the originally desired 20% overspeed criterion of 720 rev/min, so care is needed when flying the rotor in the wind tunnel. Rotor speed is not automatically controlled in the tunnel tests and so the speed changes as collective pitch is altered. A careful note of the speed has to be made when reducing the collective pitch from a high thrust setting.

Blade stability in forward flight is one possible problem which has so far not been mentioned. At an advance ratio of 0.1 the tip of the advancing blade moves with the same airspeed as the tip of a rotor in hover rotating at 660 rev/min (1 rev/min below the flutter speed). The question is whether the rotor would go unstable at advance ratios above 0.1. There has been no experimental work on blade stability in forward flight in the UK and very little world-wide. A number of flutter calculations[17,49,50] have shown that, in general, if a rotor is stable in hover, it is stable also in forward flight. There are however some forms of instability that can occur at very high forward speeds[49], although these are again only predicted

results. During the wind tunnel test of the Mk2 blade it was decided to clear the rotor to a higher forward speed before taking data at the lower speeds. This procedure avoided flying the rotor for a prolonged period near a flight condition that could be potentially dangerous.

3.2 Wind tunnel test results

The Mk2 swept tip blade was successfully flown in the RAE 24ft Wind Tunnel during December 1986. Fig 30 shows the rotor test rig installed in the wind tunnel and Fig 31 shows a closer view of the rotor. During the test there were no signs of any instabilities which tends to support the hypothesis that the blades are stable in forward flight if they are stable in hover. The only limitation during the test was that the highest advance ratio attained at 600N thrust was 0.28 due to the flap loads on the flap flexure exceeding their design load. The same limit was observed previously on the baseline blade. The wind tunnel test cases are listed in Table 6. The values of thrust and forward speed are those for a standard atmosphere (1013.25mb pressure and 288.15K temperature). The actual values of thrust and forward speed used during a test were altered to compensate for any changes in atmospheric temperature and pressure. The raw data has been processed in the way described in Appendix E.

Comparisons between the swept tip blade and the baseline rectangular blade are made in this section. The wind tunnel test cases considered first are advance ratios between 0.1 and 0.34 at 900N thrust which is equivalent to thrust coefficient/solidity of 0.0638. This is similar to a Westland Lynx AH-1 at maximum take-off weight. All of the test data are filtered during the processing by the DATAMAP plotting package using a cut off frequency of 300Hz to remove the noise from some of the signals. Also, the steady component of the signal has been removed as the datum is different for the two rotors. Both time domain and frequency domain results are presented. Comparisons have only been presented for an inboard blade station although similar results are found further outboard. The wind tunnel tests included hover although no results are presented for this case. The rotor shaft was inclined 5° forward and this produced a slow

circulation of air around the wind tunnel circuit. The exact conditions would be difficult to measure and hence the results for the two rotors cannot be compared for the hover case.

Fig 32 shows the azimuthal variation of measured flatwise bending moments on the rectangular and swept tip blades. The results shown are for four advance ratios; 0.1, 0.2, 0.3 and 0.34. The azimuthal bending moment variations are very similar for the two blades except for the lower troughs on the advancing side of the rotor disc and the higher peaks on the retreating side for the rectangular blade at the higher advance ratios. Harmonic analyses for the baseline blade, Fig 33, and for the swept tip blade, Fig 34, show that for advance ratios of 0.1 and 0.2 the magnitude and phase is similar for both blades and the signals are composed mainly of 1Ω (1 per rev.) and 3Ω . At the higher advance ratios both signals are composed mainly of 1Ω and 2Ω and the magnitude of these components is much larger for the baseline blade.

For the edgewise bending moments, Fig 35, the azimuthal variations are again similar with a strong 1Ω component. Harmonic analyses, Figs 36 and 37, reveal that the baseline blade has also a large 3Ω component and the combination of this with the 1Ω produces a similar peak-to-peak value to that of the swept tip blade.

Fig 38 shows the variation of the torsional moment. For an advance ratio of 0.1 the harmonic analyses, Figs 39 and 40 reveal that the swept tip blade has equally large 1Ω , 2Ω , 3Ω , and 4Ω components compared with a mainly 1Ω signal on the baseline blade. The peak-to-peak moment is also much larger on the swept tip blade. With increasing advance ratio the most noticeable difference between the torsional moments for the two blades is the additional nose-down moment for the swept tip blade on the retreating side of the rotor disc. On this side of the disc the blade is at a large positive incidence and hence the lift from the tip, whose centre of pressure is located aft of the shear centre of the main section of blade, forces the blade to twist nose-down. This explanation would not apply if the frequency of the first torsion mode was near the frequency of the

aerodynamic forcing - 1Ω . For the Mk2 blade the first torsion frequency is near 6Ω . If the frequencies were similar a phase lag between the forcing and the response would be expected. For the baseline blade the magnitude of the first four harmonics increases with advance ratio. For the swept tip blade a similar situation occurs except for a larger component of the higher harmonics at $\mu = 0.1$ and a decrease in 2Ω with advance ratio.

Fig 41 shows the variation of the peak-to-peak flatwise bending moments with advance ratio for both the swept tip blade and for the rectangular blade. The bending moments are lower for the swept tip blade and the difference between the two blades increases with thrust. The edgewise bending moments, Fig 42, are slightly higher for the swept tip blade, although, they are well within the design limits. The torsional moments, Fig 43, are larger on the swept tip blade at advance ratios below about 0.2, but lower at the higher forward speeds. The difference between the two blades again increases with thrust. The azimuthal variation of the torsional moment on the swept tip blade for different advance ratios, Fig 44, shows that there is a change in the shape of the waveform, particularly on the retreating side of the rotor disc, with advance ratio. The peak-to-peak moment is defined by the difference in moment at 280° azimuth with that at 340° azimuth at $\mu = 0.1$. At higher forward speeds the peak-to-peak moment is defined by the difference between the nose - down moment at 140° and the moment at 300° . There is also a change with advance ratio in the azimuthal position of the second nose - down excursion. Comparing these results with those from the tests on the rectangular blade, Fig 45, the very large trough on the advancing side of the rotor disc is the dominant feature. The torsional moment associated with this trough is used in the calculation of the peak-to-peak moment for all advance ratios tested.

The blade pitch (θ) at any point in azimuth is defined by:

$$\theta = A_0 + A_1 \cos \psi - B_1 \sin \psi$$

where A_0 is the collective pitch, A_1 is the magnitude of the lateral cyclic and B_1 is the magnitude of the longitudinal cyclic. Due to the 90° phase shift between the force and displacement of a rotor blade, a lateral cyclic pitch change changes the longitudinal trim of the helicopter. Similarly, a longitudinal cyclic pitch change affects the lateral trim.

Figs 46, 47 and 48 show the coefficients A_0 , A_1 and B_1 respectively, for both the baseline rectangular blade and for the Mk2 swept tip blade. The pitch angles are plotted against advance ratio and the results for four thrust cases are presented. The results were obtained by extracting the swash-plate actuator positions from the data files containing the strain gauge data. The data from the cyclic calibrations (see Appendix F) was used to convert the actuator displacements to blade pitch angles.

Fig 46 shows that the collective pitch for both blades changes with forward speed in a similar way to the change in power with advance ratio[51]. If the power is increased without any change to the collective, the rotor will rotate faster resulting in an increase in thrust. During the wind tunnel experiment the rotor speed is kept constant at its original value and therefore the collective pitch has to be increased to reduce the speed. This causes the thrust to increase further and the rotor speed decreases due to the higher induced drag. The opposite would happen if the power is decreased. The collective pitch and the power setting thus have the same trend. For the pseudo-hover cases, the collective pitch is higher for the swept tip blade than for the rectangular blade. The tip of the rectangular blade is usually at a positive incidence in hover and by introducing tip sweep the positive lift on the swept tip forces the blade to twist nose-down and hence reduces the total lift produced by the rotor. The collective then has to be increased so that the rotor produces the required thrust. About $\mu = 0.1$, the collective is the same for both blades and at higher advance ratios the baseline blade has the higher value. Above $\mu = 0.2$ the difference in collective between the two blades is fairly constant with advance ratio for a given thrust. This difference is however much larger for the highest thrust of 1050N.

Fig 47 shows a plot of the coefficient A_1 vs advance ratio. A_1 should be nominally zero in hover and as no values had been recorded between hover and $\mu = 0.1$, an interpolation between the two advance ratios could not be justified. The results have therefore been plotted from $\mu = 0.1$. The data contained much scatter suggesting that the longitudinal trim of the rotor could have changed during the few minutes of running time required to acquire the 16 revs of data. This could be due to the wind tunnel not reaching its steady state speed and the longitudinal trim being more sensitive than either the lateral trim or the collective. For the lowest advance ratio plotted, the magnitude of A_1 is larger for the rectangular blade and the difference increases with thrust. At the highest advance ratio the situation is reversed with the swept tip blade having the largest A_1 magnitude. The difference in A_1 between the two blades increases rapidly with advance ratio except for the highest thrust.

Fig 48 shows the coefficient B_1 plotted against advance ratio. As the forward speed increases the lift on the advancing blade increases whilst the lift on the retreating blade decreases. To maintain a balance in roll the rotor is trimmed in the lateral direction by changing the longitudinal cyclic pitch. This is true for both blades. For all thrusts the swept tip blade requires a larger trim angle than the baseline blade although the difference between the two blades is small at the highest thrust. This is again due to the higher twist on the swept tip blade.

The final part of the wind tunnel test programme was to observe the blade loads as retreating blade stall started to develop. The usual sign that the blade is stalling is the gradual development of a large amplitude, high frequency oscillation in pitch on the retreating side of the rotor disc. For a fixed advance ratio the thrust was gradually increased until load limits were achieved. For these cases the combined flap and lag bending moment on the swept link reached its limit first. For increasing advance ratios the maximum thrust attained decreased as both thrust and forward speed affect the swept link bending moment. On examining the torsional moments at the root of the baseline blade there were some signs that the blade was beginning to

stall at an advance ratio of 0.1. As the thrust increased the magnitude of the torsion response increased at all azimuth stations. For the highest thrust, additional changes in response were observed around 270° azimuth. For the swept tip blade, these changes were however not observed. Results showing this are not presented in this section although similar results are shown in section 4.2.1 for the Mk3 swept tip blade.

3.3 Comparisons between experiment and theory

Comparisons between the calculated and measured bending moments on the Mk2 swept tip blade are made in this section. The results presented are only for advance ratios above 0.1 as the predictions from the loads programme are not reliable at lower forward speeds due to the shortcomings in the prescribed wake model. Two sets of calculated results are presented. The first set was obtained using the J134 modes programme and the R150 loads programme discussed in Appendix A. The second set used a different modes calculation which enabled the use of a more accurate hub representation. The R150 loads programme was also used for these calculations. All of the results had been filtered at 300Hz and the steady loads had been removed.

3.3.1 Correlation using the RAE/WHL coupled modes analysis

The rotor was trimmed during the wind tunnel experiment by minimising the 1 Ω signal on the flap flexure. Following the experiment a harmonic analysis of the flap flexure signal using the DATAMAP program gave the residual force. This force was then used in the R150 program as the aim in the trim routine. For a hinged rotor the coning angle is measured in the experiment and the residual 1 Ω flapping angle is used in the trim routine. Similarly for a hingeless rotor a root flatwise bending moment is measured and the residual 1 Ω bending moment is used in the trim. In both cases only one variable has to be matched by the prediction program i.e. the coning angle or the root flatwise bending moment. The trim for the model rotor is determined by the vertical force in the flap load path, the flap force being dependent not only on the blade lift, but also on the centrifugal load which is

a function of the coning angle. For the model rotor blade the coning angle and the blade lift have to be predicted well to obtain the desired trim state. Previous tests on a rectangular blade mounted on the DLP hub[41,42] had shown that a trim using the flatwise bending moment of the swept link gave similar blade stresses to a trim using the flap flexure, confirming that the coning angle and the blade lift were predicted well.

A list summarising the input data for the J134 program is shown in Table 4 for the baseline rectangular blade. The modifications made to this data for the swept tip blade are shown in Table 7.

A few checks were made on the blade loads at other parts of the test envelope following the stability assessment of the Mk2 blade. For some of the cases the modes programme failed, due to large deflections appearing in the calculation of the steady state position of the blade. The problem was found to be with the initial deflection of the blade tip. The input data for this programme contains the global coordinates of the rotor system. With the rotor twisted, the tip of the blade has a large vertical deflection due to the sweep. The programme was modified by WHL so that large deflections would be suppressed in the first stage of the calculations.

The measured and calculated flatwise bending moments at 37% radius are shown in Fig 49 for four advance ratios at 900N thrust. The correlation between the two sets of results is reasonably good particularly for the lower forward speeds. For the higher advance ratios there is a discrepancy on the advancing side of the rotor disc between 60° and 180° azimuth. Fig 50 shows the harmonic analysis of the predicted results and Fig 51 shows the corresponding harmonic analysis of the experimental results. The discrepancies in the prediction are mainly the underestimation of the magnitude of the 12 component although the predicted phase is good. One feature which has been predicted by the theory but is not present in the experimental results, is the high frequency oscillation on the advancing side of the rotor disc at the higher advance ratios. This oscillation is due to the prediction of the close proximity of the blade to the vortex

shed from the tip of the preceding blade. Fig 52 shows the trajectories of the tip vortices shed from the rotor at an advance ratio of 0.3. The No. 3 blade passes close to the tip vortex from the preceding No. 2 blade. Another predicted interaction takes place further inboard on the same blade, but the effect on the blade loads would be much smaller. This is because the vortex has been displaced downwards by the downwash from the No. 2 blade. There are some signs at other spanwise stations, not presented here, that the vortex is affecting the blade flatwise bending moment but the effect is less noticeable. This result shows the importance of being able to predict accurately the position of the tip vortex.

Corresponding plots for the edgewise bending moments are shown in Fig 53 and the harmonic analyses in Figs 54 and 55. The main contribution to the edgewise response comes from 1Ω and there are relatively small higher harmonic components. For increasing advance ratio the error in the amplitude of the 1Ω component increases although the phase difference between the experiment and theory decreases. At the higher forward speeds there is a pronounced 7Ω oscillation in the predicted results which arises from the second lag mode at 7.09Ω - Fig 28.

Fig 56 shows the comparison of torsional moments. At an advance ratio of 0.1, the correlation between the measured and predicted torsional moments is fair but rapidly deteriorates at the higher forward speeds. The main problem seems to be a discrepancy in the 1Ω and 2Ω components - Figs 57 and 58. There is also a significantly higher harmonic content in the predicted results which arises from the second torsion mode.

Figs 49-58 have illustrated the differences between the experiment and theory for a number of advance ratios at a fixed thrust of 900N. Figs 59-61 show the results for four thrust cases at an advance ratio of 0.2. The agreement between the two sets of results for the flatwise bending moments, Fig 59, is fairly consistent throughout the thrust range. This is also true for the edgewise

bending moments, Fig 60. For the torsional moments, Fig 61, the agreement improves with increasing thrust, especially between 40° and 120° azimuth.

3.3.2 Correlation using an improved hub representation

The dual load path hub used in the wind tunnel tests, Figs 3-5, is a difficult system to model accurately without the use of finite element techniques. A previous analysis of the results from a wind tunnel test on the baseline rectangular blade[52] has suggested that the representation of the secondary load paths by linear springs, Fig 6, is too simplistic. At present no account is taken of the fact that the inertia of the frame surrounding the coupling bearing is present only in flap and not in lag. Also, the rotational inertia about the flap axis of the coupling bearing and frame is not represented.

The coupled modes program (J134) used in the above analysis has been modified by WHL to analyse bearingless rotors for which a full representation of the two load paths can be incorporated into the rotor model. Fig 62 shows a typical example of a bearingless rotor. One of the load paths represents the flexible beam joining the blade to the hub (flexure 1) whilst the second load path (flexure 2) is used to model a torque tube connecting the control system to the blade root. Stiffnesses for the six degrees of freedom at the root of each flexure are also defined. For flexure 2 an additional six flexibilities and six inertias at the end of a rigid rod can be used to represent the control system. Although the dual load path hub is not a bearingless rotor, the two load paths in the new modes programme can be used to produce a more accurate hub representation than used in the previous program, Fig 63. The hub flexures have been arranged so that the blade pitch is controlled through flexure 2 which also carries the centrifugal load. Flexure 1 carries the bending loads and therefore replaces the two flap flexures. The spanwise stiffness properties of the flexure are calculated as if there was only one centrally positioned flap flexure. The rotational inertia of the coupling bearing and frame cannot however be incorporated in the

program. One difference between the bearingless rotor and the dual load path hub is that the elements joining the blade to the dual load path hub do not carry any torsional loads. A flexible element between the tips of the two load paths was introduced by WHL so as to enable the blade flatwise and edgewise loads to be transferred to flexure 1, whilst the torsional loads are reacted by flexure 2.

The input data for this new program, which is known as J146, is very similar to that used for J134. The main difference is that the structural properties for the two load paths are separated from the blade data. The properties that are defined for the hub are the same as for the main section of blade. A list of the structural properties for the blade can be found in Table 8 and the properties for the two flexures are in Tables 9 and 10. A description of the various blade stations is in Table 11.

The mode shapes calculated for the swept tip blade using the modified program are shown in Fig 64. Comparing these with the modes produced by the J134 program, Figs 28 and 29, the most significant change is the increase in the second lag frequency from 7.092 to 7.52 and a reduced coupling between the lag mode and the other modes.

The rotor was trimmed to the residual 12 bending moment on the flap flexure for the analysis using J134. For J146 this was no longer possible as the trimming had to be done using flexure 2. For this analysis the residual 12 bending moment was taken from the swept link. The disadvantage in using an element further outboard is that the bending moments from the higher order modes start to become significant and the 12 trim may no longer be valid. Calculations using J134 and R150 showed that there were only small differences between the two types of trim.

The measured and predicted flatwise bending moments are shown in Fig 65 with the harmonic analysis in Fig 66. There is generally very little change between these results and those using J134, Fig 49. There is some improvement in the 12 amplitude at the high advance ratios which improves the prediction between 100° and 180° azimuth.

There is, however, an undesirable increase in the 3Q component at the lowest forward speed. For the edgewise bending moments, Figs 67 and 68, the correlation is much worse in terms of magnitude. The phase difference between the experimental and predicted 1Q component is at least 50° compared with about 20° for the predictions using J134. A further analysis of the first lag mode at low rotational speeds revealed that its frequency approached zero at 0 rev/min. This result indicates that there is a possibility that an error was introduced whilst the J146 program was being modified to allow for the flexibilities between the two load paths. Finally, Figs 69 and 70 show the torsional moments. On the advancing side of the rotor disc the magnitude of the nose - down peak is in good agreement especially at the higher advance ratios and there is also a significant improvement on the prediction made by the previous analysis, Fig 56. The improvement over the previous prediction arises from the increase in magnitude of the first four harmonics and an improvement in the phasing of the third harmonic. There is however a need for improvement in the predicted phasing of the peaks on the retreating side of the disc. The error could be due to the incorrect phasing of the fourth harmonic.

The gross effects of sweep back are reasonably well represented although there is room for improvement in the predicted variation of blade torsional moment. This gives confidence in the belief that the predicted effect of sweep back on blade twist is being achieved. Fig 71 shows the predicted variation in the twist of the swept tip blade relative to the twist of the baseline rectangular blade, the twist being defined as the difference between the blade tip and blade root geometric incidence. On the advancing side of the rotor disc, 90° azimuth, the twist decreases with advance ratio whilst on the retreating side, 270° azimuth, it increases at advance ratios above 0.18. This is one of the aims in the design of an aeroelastically adaptive rotor to improve the aerodynamic performance of the rotor.

4 DESIGN AND TESTING OF THE Mk3 BLADE

The design and testing of a rotor blade incorporating a swept tip was discussed in the previous two chapters. The initial design was found to be unstable showing that blade stability is important in blade designs that have a centre of gravity offset aft of the shear centre. The flutter speed was delayed to a higher rotational speed by increasing the torsional stiffness and by using chordwise mass balancing. The addition of a mass near the leading edge at the blade/tip junction nearly balanced the aft centre of gravity of the swept tip thus greatly reducing the pitch-flap inertia coupling. Comparisons were made between the experimental results on a stable second blade design and those predicted by the RAE/WHL coupled modes analysis. The results showed that a good prediction of the blade root flatwise and torsional moments was achieved. The accurate prediction of these moments is particularly important when investigating aeroelastically adaptive rotors. The prediction methods can now be used to aid the design of blades incorporating further features of the aeroelastically tailoring concept.

The further reduction of blade stresses was one possible aim in the design of the Mk3 blade although a significant reduction in vibration had already been demonstrated in the design of the Mk2 blade. The design of a blade for lower stresses without the benefits of optimisation programs would be difficult and time consuming. Helicopter rotor blades incorporating aeroelastic tailoring to reduce vibration can include large inertia couplings. This can be achieved by offsetting the centre of gravity from the shear centre so as to produce a large flap-torsion coupling. It would therefore be a useful exercise to validate the prediction programs for this type of blade.

The leading edge extension in the design of the BERP3 blade, Fig 72, was introduced to provide a balance to the pitching moment produced by the lift from the aft swept tip. The point of balance is the shear centre of the main section of blade which is also near the aerodynamic centre. A useful exercise would be to move the local shear centre position aft to provide a balance to the aerodynamic centre

offset in place of the leading edge extension. For a BERP3 type rotor blade flown with a representative tip speed, this modification could have a beneficial effect on noise; the noise being a function of tip volume. The profile power would be reduced also, and the aft sweep of the shear centre could improve blade stability by reducing the need for large chordwise mass balancing.

This chapter describes the design and testing of a new model blade, the Mk3 design, incorporating a large flap-torsion inertia coupling whilst maintaining a similar dynamic twisting to that of the Mk2 blade. A study was made first of the effect of pre-twist on blade stability and blade root stresses. This was followed by a study of the effect of moving the shear centre of the blade aft and the shear centre of the tip forward. The essential results from comparisons between the stresses on the new blade with those on the Mk2 swept tip are presented following a discussion on the final specification of the Mk3 blade. Comparisons are then made between the measured and predicted moments.

4.1 The Mk3 swept tip blade design

The main aim in the design of the rectangular blade[44] has been to reproduce the non-dimensional modal frequencies of a full size helicopter blade. A modern aerofoil section is used also, RAE 9642, but the twist of the rotor blade is low compared with full size blades. The twist for the model blade is about 5° compared with 8° for the Westland Lynx or 18° for the Sikorsky UH-60A Black Hawk. High twist is beneficial in hover but there are large penalties such as large oscillatory control loads in high speed flight. The blade static twist has a significant effect on the spanwise lift distribution which could adversely affect blade stability in addition to the blade root stresses. The consequences of increasing the twist for both swept tip and rectangular blades are addressed in section 4.1.1 and this is followed, in section 4.1.2, by an investigation of the effect of sweeping back the shear centre on blade response, performance and stability.

4.1.1 The effect of blade twist on stability and moments

A previous study at the RAE has shown that it is possible to manufacture a model blade with a higher twist than 5° whilst using the same blade moulds. The torsional stiffness of the mould is reduced by removing large portions of the outside of the mould leaving only narrow equally spaced sections in the chordwise direction. A new twist can be applied to the blade by adding tapered spacers between the narrow sections and the press in which the blade is cured. A similar method can be applied to the swept tip blade moulds. The theoretical analysis of a blade with the twist doubled to $8.8^\circ/\text{m}$ is described below. The effects of this modification on both the Mk2 swept tip blade and the baseline rectangular blade are discussed. The computer programs used are the J146 modes analysis, R150 performance calculation and the stability program mentioned in the previous two chapters.

Fig 73 shows the effect of doubling the blade twist on the peak-to-peak flatwise bending moments for advance ratios of 0.1, 0.22 and 0.34. All three plots show that a higher twist will increase the flatwise bending moments, the increase being larger for the rectangular blade than for the swept tip blade. This is also true for the edgewise bending moments, Fig 74, although the differences between the low and high twist blades are much smaller than for the flatwise bending moments. For the torsional moments, Fig 75, twist has a negligible effect on the spanwise peak-to-peak moments at the lowest advance ratio. At $\mu = 0.22$ the moments are reduced by the higher twist blade inboard of $0.6R$, the largest effect being on the swept tip blade. Outboard of $0.6R$ the effect is reversed and twist increases the moments on both blades. At the highest advance ratio the moments are larger over the whole of the blade. The root moment on the swept tip blade has not changed whereas it has increased by about 30% on the rectangular blade.

The Table below shows the predicted total rotor power for both the swept tip blade and the baseline blade with standard and high twist.

Total rotor power (KW)				
		Baseline blade		Swept tip blade
Twist	4.4°/m	8.8°/m	4.4°/m	8.8°/m
Advance ratio				
0.10	5.7	5.6	5.6	5.6
0.22	5.7	5.7	5.5	5.5
0.34	7.1	7.1	6.9	6.9

Comparing the results for each blade, the effect of increasing blade twist is negligible for a given advance ratio. Comparing the baseline blade with the swept tip blade, the former requires more power than the latter. The difference in power between the two blades is also larger at the higher forward speeds. The probable reason why the differences between the two blades are small is that the tip speed of the rotors is low compared to a full size rotor. At high advance ratios the rotor power is very sensitive to the incidence of the blade tip on the advancing side of the rotor disc. A high positive or negative tip incidence leads to a high induced power on a full size rotor. The presence of shock waves on the tip surface causes separation of the aerofoil boundary layer which results in a large increase in drag and an increase in the rotor power. The results presented in the previous chapter for the theoretical analysis of the blade dynamic twist, show that the aft sweep is forcing the blade tip towards zero incidence. A large reduction in the rotor power would be anticipated therefore for a full size rotor at high forward speeds. The aspect of rotor power is considered in greater detail in an investigation of the effects of aeroelastic tailoring on a full size rotor in Section 5.

Fig 76 shows the effect of increased twist on the damping of the 2F and the 3F/1T modes on the swept tip blade. For the blade with standard twist, the upper figure, the flutter speed is 755 rev/min

compared with 710 rev/min for the higher twist blade, the lower figure. This is a significant reduction and such a blade could not be flown in the wind tunnel.

The above results show that an increase in twist on the swept tip blade does not have an adverse effect on the gains observed with the $4.4^\circ/\text{m}$ twist. In some cases such as at high advance ratio, the increase in the torsional moments for the baseline blade significantly enhances the relative performance of the swept tip blade. The results also confirm the findings of Keys et al[29] that live twist reduces the cruise penalties of high twist rotor blades. The only disadvantage of increasing blade twist is the large reduction in flutter speed and for this reason, a twist of $4.4^\circ/\text{m}$ was retained in the Mk3 blade design used to investigate shear centre sweep.

4.1.2 Investigation of shear centre sweep

A coupling between the flatwise and torsion motions was originally intended in the design of the Mk1 swept tip blade by using the blade inertia. The coupling was to have been achieved by positioning the centre of gravity of the swept portion aft of the shear centre of the main section of blade. The coupling did however lead to a blade instability and the flutter had to be suppressed by the addition of a large mass at the leading edge near the blade/tip junction. The mass had the effect of decoupling the flatwise/torsion response and hence delaying the onset of flutter to a higher rotational speed.

An alternative way of introducing a flatwise/torsion coupling was to position the centre of gravity forward rather than aft of the shear centre. In that case the shear centre was moved aft whilst keeping the centre of gravity near the aerodynamic centre on the main section of blade.

The configuration shown in Fig 77 was chosen as the first step in looking at a suitable blade design with a swept shear centre. The shear centre on the main section of blade is swept back by an angle Λ

so that the shear centre is at $0.336c$ at the blade/tip junction. This is the same chordwise position as the shear centre on the Mk2 blade tip. The shear centre is swept forward on the outboard portion of the blade meeting the quarter chord at the blade tip. For this particular case $\Lambda = 0.63^\circ$ (Mk3(i) blade).

J146 was used for the prediction of the blade mode shapes and frequencies. The input data were modified to take into account the sweep of the shear centre. As described in section 2.2.2 the overall geometry of the blade is defined by using the global coordinates of the shear centre locus. Careful re-calculation of these coordinates was necessary as the tip sweep, as far as the shear centre is concerned, was 20° minus the angle Λ and minus the sweep of the tip shear centre. The offset of the centre of gravity from the shear centre and the offset of the aerodynamic centre from the shear centre was changed also. The former of the offsets also changes the blade radius of gyration distribution Km_2^2 , although this does not vary in the same way along the blade span as the other variables, because the radius of gyration is a function of the square of the offset of the centre of gravity from the shear centre. Four additional stations along the blade were introduced therefore so that the spanwise variation in the radius of gyration would be represented more accurately.

The loads for the Mk3(i) blade were calculated using the program R150 described earlier. Fig 78 shows the predicted bending moments and torsional moments at the blade root for a thrust of 900N and an advance ratio of 0.34. The results are compared with those predicted for the Mk2 swept tip blade. For the flatwise bending moments and torsional moments the blade wave forms are very similar to that for the Mk2 blade. For the edgewise bending moments there is a higher 72 component for the Mk3(i) blade. The peak-to-peak flatwise and edgewise bending moments are slightly increased whereas, the torsional moment is reduced. Fig 79 shows the percentage critical damping for the two modes that converge for both the Mk3(i) blade design and for the Mk2 blade. The results show a significant increase in flutter speed from 755 rev/min to 780 rev/min. Apart from the

edgewise moments which are of little interest in the present analysis, the change in the peak-to-peak moments is less than 9%. The accuracy of the prediction programs was shown in the previous section to be of the same order and therefore a much larger change in the wave form was required to make the comparison with theory a valid exercise.

Increasing the sweep angle Λ to 1.0° had only a small effect on the blade root stresses but the flutter speed was further increased to 794 rev/min. Comparing the amount of dynamic twisting of the blade with that for the Mk2 blade, the twist had increased slightly on both the advancing and retreating sides of the rotor disc. From the performance point of view this meant a slight improvement on the retreating side with a slight degradation on the advancing side. A further increase in Λ to 1.1° gave problems with the stability of the performance program R150.

The next step was to look at the sensitivity on blade stresses and stability to the position of the shear centre on the tip itself. With the shear centre at the blade tip positioned 5mm forward of the quarter chord, the shear centre at the blade/tip junction was progressively moved forward. The shear centre for the inboard section was swept 1.0° . The effect of this modification was to reduce slightly the blade flutter speed. The blade response was significantly changed on the retreating side of the rotor disc for the flatwise bending moment and between 0° and 180° and between 240° and 360° for the torsional moment. However, the dynamic twisting of the blade was similar to that observed for the blade with just the inboard shear centre swept.

Table 12 lists the properties of a blade that are suitable for the Mk3 blade design (Mk3(ii)). Most of the properties such as the stiffnesses are the same as for the Mk2 blade. The main expected differences between these preliminary specifications and the final ones are the estimated blade radius of gyration $K_{y_2}^2$ and in the shear centre position. At the blade root the shear centre is 35.1mm from the leading edge and sweeps back 1° to 54mm at the blade/tip junction. On the swept section the shear centre sweeps from 39mm at the blade/tip

junction to 30mm at the tip, compared to a constant 47mm for the Mk2 blade. This specification was used as a starting point for the detailed design of the Mk3 blade discussed in the next section.

4.1.3 The final design

A number of blade samples had been manufactured to represent the type of fibre layup anticipated to fulfil the specification mentioned in the previous section. The samples had constant structural properties along their length to facilitate the measurement of the properties. For the main part of the blade it was assumed that the additional leading edge layers of glass fibre (Fig D3) were reduced in width towards the tip. This achieves a sweep back in the shear centre. To compensate for the gradual reduction in the edgewise stiffness, layers of fibre were added onto the trailing edge. Any reduction in the flatwise stiffness could also be compensated for by adding layers of fibre above and below the shear centre. For the tip of the inboard section of blade, it had been found that to achieve the required shear centre position, the layers of carbon fibre on the shear centre had to be swept back further towards the trailing edge.

The repositioning of the shear centre at 30mm from the leading edge for the outboard end of the tip in addition to the sweep of the shear centre had been found to be very difficult. With the aid of the modes and performance analysis and the stability programme a number of designs were evaluated with various positions of the shear centre. For these cases the shear centre ran parallel to the leading edge. One possible solution for an alternative design was to position the shear centre at 38mm from the leading edge (Mk3(iii) blade). Fig 80 shows the predicted bending moments and torsional moments for the Mk3(iii) blade. Both the flatwise and torsion responses are fairly similar to the Mk3(ii) blade but the responses have changed significantly compared with those presented in Fig 78 for the Mk(i) blade. There is a large reduction in the peak-to-peak value of the torsion response. The flutter speed is 776 rev/min or 21 rev/min above that for the Mk2 blade.

Further blade samples were made and the arrangement of fibres towards the trailing edge introduced a significant shift aft in the centre of gravity. To compensate for this an additional mass was added at the leading edge thus restoring the centre of gravity to its original position. Table 13 shows the measured properties and compared to the specified values. The results calculated using these new values showed that the stresses were unchanged although the stability was reduced to a value much lower than that of the Mk2 blade. A further investigation revealed that the stability problem was due to the additional mass on the swept tip.

Fig 81 shows the predicted blade root bending moments and torsional moments at 900N thrust and at $\mu = 0.34$. In this example (Mk3(iv)) the tip was replaced with the tip from the Mk2 blade and the properties of the inboard section were the same as those measured and presented in Table 13. The peak-to-peak flatwise bending moment is reduced by 14% compared with the Mk2 blade. This represents a total reduction of 43% from the baseline blade. The torsional moments is reduced by 22%, or 41% relative to the baseline blade. The flutter speed is predicted to be 750 rev/min, slightly below the Mk2 blade flutter speed and the dynamic twisting of the blade is slightly reduced compared with the Mk2 blade. The performance of the Mk3(iv) blade is acceptable therefore when compared with the requirements for the Mk3 blade mentioned earlier.

Fig 82 shows the mode shapes and frequencies for the Mk3 blade, previously referred to as the Mk3(iv) blade. Comparing this figure with Fig 64, which shows equivalent plots for the Mk2 blade, all of the frequencies are reduced. The largest reduction is however only 7.7%. This reduction in the modal frequencies is due to the increase in the flap, lag and torsional inertias. The mode shapes have not changed significantly apart from the second lag mode. The coupling between the lag motion and the flap and torsion motions is reduced by the Mk3 blade and the dynamic twisting of the blade is reduced by about 20%.

The full manufacturing details and specification for the Mk3 blade can be found in Appendix G.

4.1.4 Blade flutter tests

During the development of the Mk2 swept tip blade exploratory flutter tests were made on the single blade test facility, Section 3.1, as the flutter prediction programme was still under development. A further flutter test was made prior to the installation of the rotor test rig in the wind tunnel.

The current version of the flutter programme was found to overestimate the flutter speed of the Mk2 blade although the nature of the flutter was predicted well. The original flutter test on the Mk2 blade prior to the installation of the rotor in the wind tunnel, was used only to determine the flutter speed and not to obtain the modal frequencies and dampings throughout the speed range. The flutter test on the Mk2 blade was repeated therefore on the single blade rig to determine the modal frequencies and dampings. The test was performed also for the new Mk3 blade. The results of the experiments along with the theoretical predictions are presented next.

Fig 83 shows the predicted and experimental modal frequencies and percentage critical dampings for the Mk2 swept tip blade. Only the second flap and the highly coupled first torsion/third flap modes are presented. For the modal frequencies there is good agreement below 450 rev/min. Above this rotational speed, the two modes converge sooner than predicted. This is reflected also in the damping of the modes presented in the lower figure. The actual flutter speed is 686 rev/min, about 70 rev/min below the predicted speed. Fig 84 shows similar plots for the Mk3 blade and it was anticipated that the flutter speed would drop only 5 rev/min between the Mk2 and Mk3 blades. The experiments show that this difference is nearer 15 rev/min. In view of this larger change in flutter speed a further flutter test was made on the wind tunnel test rig with the Mk3 blade. During the analysis of the Mk2 blade it was demonstrated that there

was a reduction of 30 rev/min in the flutter speed between the flutter test rig for the single blade and the wind tunnel test rig. The second flutter test confirmed this for the MK3 blade.

The Mk3 blade was considered safe to fly in the wind tunnel inspite of the 15 rev/min lower flutter speed mentioned above. The previous wind tunnel test of the Mk2 blade showed that there were no signs of flutter despite the fact that the tip region of the blade on the advancing side of the rotor disc, was experiencing higher velocities at its normal rotational speed than in hover at the flutter rotational speed. The strict monitoring of the rotor speed whilst lowering the thrust was however even more important for the Mk3 blade than for the Mk2 blade.

4.2 Analysis of the wind tunnel test results

The Mk3 swept tip blade was flown successfully in the RAE 24ft wind tunnel during September 1989. The experimental set up and the flight test conditions were the same as for the Mk2 blade. The only part of the test programme, Table 6, that was not achieved was the highest advance ratio at 600N thrust. A similar problem was observed on the Mk2 blade which attained an advance ratio of 0.28 before the flap flexures reached their limit load. The higher speed of $\mu = 0.32$ was attained by the Mk3 blade. The thrusts achieved during the stall tests prior to encountering a bending moment limit on the swept links were also higher for the Mk3 blade compared with both the baseline blade and the Mk2 blade.

The results from the wind tunnel tests are presented in the sections following. Only a limited number of results are presented to show the general behaviour of the blade compared with the Mk2 swept tip blade. The first section compares the blade root bending moments and torsional moments on the two blades. Comparisons are made at one thrust and at four advance ratios spanning the wind tunnel test envelope. Both time domain and frequency domain results are presented. The section contains also the variation of the peak-to-peak moments

with advance ratio for various thrusts. The results are again compared with those for the Mk2 blade. The section is concluded by looking at some of the results from the high thrust cases.

Comparisons are made between the wind tunnel test results and those predicted using the modes program, J146, and the performance program R150 in Section 4.2.2.

4.2.1 Comparisons between the Mk3 blade and the Mk2 blade

Fig 85 shows the flatwise bending moments measured at the blade root for both the Mk2 and Mk3 swept tip blades. The plots show that the bending moments are very similar both in the type of response and in magnitude. The harmonic analysis, Fig 86, does not reveal any additional information. There is however a large phase difference in the second harmonic at $\mu = 0.1$ compared with the previous blade design, Fig 34. For the edgewise bending moments, Figs 87 and 88, the responses for the Mk3 blade have a higher 3 Ω component than those found on the Mk2 blade. They very closely resemble the responses for the baseline blade Figs 35 and 36. Finally, for the torsional moments Figs 89 and 90, the responses are again very similar to those of the Mk2 blade.

Fig 91 shows the peak-to-peak flatwise bending moments measured at 30% radius. The results for both the Mk2 and Mk3 blades are presented. For most thrust cases and advance ratios the bending moments are reduced by the Mk3 blade although the reduction is less than that observed whilst comparing the Mk2 blade with the baseline blade, Fig 41. For the edgewise bending moments measured at 37% radius, Fig 92, the values are reduced generally by the Mk3 blade at 600N thrust. For the higher thrusts the bending moments are increased at advance ratios below about 0.2 and reduced at higher advance ratios. Fig 93 shows the torsional moments at 30% radius. Compared with the Mk2 blade, the Mk3 blade torsional moment has a larger variation with advance ratio. At the lower advance ratios the moments are generally higher on the Mk3 blade and the moments are lower at the higher forward speeds for the same reasons as discussed in section

3.2. Comparing this figure with Fig 43, the point at which the Mk3 blade has lower moments than the baseline blade is nearer $\mu = 0.2$ rather than $\mu = 0.25$ for the Mk2 blade.

The results presented above are for the blade root. Similar trends have been observed at blade stations further outboard.

The final comparisons made are between the Mk3 swept tip blade and the baseline blade for the high thrust cases. The aim of this part of the experiment, as explained earlier, was to increase the thrust whilst maintaining a constant wind tunnel speed. The experiment was stopped once the rotor limits had been reached. The limit for all of the tests was the combined flatwise and edgewise moment on the swept link. The moments on the swept link depend not only on the thrust but to a greater extent on the advance ratio. The highest thrust attained was therefore at the lowest forward speed, $\mu = 0.1$.

Plots for the root torsional moments on the baseline blade are shown in Fig 94. The thrust ranges from 1089N to 1374N. The only change in the response during the thrust increase is between 200° and 300° azimuth. This is also true for the torsional moments on the Mk3 swept tip blade, Fig 95. The thrust for this blade reaches 1481N. Both figures do not show any signs of stall flutter which is expected to appear as large oscillations of blade pitch starting on the retreating side of the rotor disc.

4.2.2 Comparisons between experiment and theory

Comparisons between the experimental results and those predicted by the J146/R150 programs are presented in this section for the Mk3 blade. Theoretical results have been presented already in Fig 81, but those were calculated assuming a zero residual bending moment on the swept link. The results discussed below are for a rotor trimmed to the residual 12 bending moment found from a Fourier analysis of the experimental results, i.e. the same method as used for the Mk2 blade comparisons. Comparisons between experiment and theory are

presented for four advance ratios at 900N thrust and for the same blade station as for the Mk2 blade.

Fig 96 shows the measured and predicted flatwise bending moments for the Mk3 blade. The moments are for a 900N thrust and for 37% blade radius. Figs 97 and 98 show the corresponding harmonic analyses for the predicted and measured bending moments respectively. The predictions are very good except for the lowest advance ratio and are an improvement over the predictions for the Mk2 blade, Fig 65. For an advance ratio of 0.1 there is a much smaller 1 Ω component in the predicted result. This reduced 1 Ω component was observed also for the Mk2 blade although it was also evident in the higher advance ratios. The phasing of the first three harmonics is also well predicted.

Fig 99 shows the comparisons between the measured and predicted edgewise bending moments with the harmonic analyses in Figs 100 and 101 and, as expected, there is a gross underestimation of the 1 Ω and 3 Ω components. The problem of the prediction of the frequency of the first lag mode at low rotational speeds, mentioned in section 3.3.2, has not been resolved.

The torsional moments are shown in Fig 102 with the harmonic analyses in Figs 103 and 104. The accuracy of the predictions is generally changed very little compared with similar results presented for the Mk2 blade, Fig 69. The prediction of the magnitude and the phasing of the first three harmonics is reasonably good except for the magnitude of the third harmonic at $\mu = 0.1$. At $\mu = 0.3$ the second nose down pitching moment excursion is reduced in magnitude and it is lost amongst the higher frequency response. There is still a problem with the prediction of the position of the second nose-down peak on the retreating side of the rotor disc at $\mu = 0.34$.

A comparison of the above results with those obtained during the design of the Mk3 blade, Fig 81, shows that the predicted large reduction in the peak-to-peak flatwise bending moments and torsional moments is not evident. One difference between the predicted results at the design stage and the results presented for comparison with the

experimental results, is that the latter includes a trim of the rotor to the trim observed in the wind tunnel experiment. This effect should be very small and could not account for the large discrepancy. Following a detailed inspection of the input data to the loads prediction programs, the only difference found between the J146 input data for the blade, Table G1, and the data used in the comparisons with experimental data was the specification of the collective pitch. The collective pitch can be either included in the data table for the spanwise twist distribution, included as a separate entry with zero root incidence in the twist distribution, or a combination of both. A further investigation using the two types of collective specification on both the Mk2 and Mk3 blades had been undertaken. The results showed that the change in magnitude of the bending and torsional moments between the two blades was much larger when specifying the collective using the third option than whilst using the second option, suggesting an error in the prediction programs. The test was repeated for the baseline rectangular blade and similar results were observed although the bending and torsional moments were in agreement when using J134 to predict the mode shapes.

Fig 93 shows that a large difference between the Mk2 and Mk3 blade torsional moments occurs at $\mu = 0.24$, 900N thrust. To check if the prediction programs do in fact predict the effects of sweeping the shear centre, further comparisons between the experimental and predicted results are made for this advance ratio and thrust. Fig 105 shows the experimental and predicted torsional moments for the Mk2 and Mk3 swept tip blades. The corresponding harmonic analyses are shown in Fig 106. The figures show that for both the Mk2 and Mk3 blades the predicted and experimental results are very similar. For the experimental results the main difference is the reduction of the second harmonic for the Mk3 blade. This is not evident in the predicted results. The theory predicts a higher third harmonic for the Mk3 blade. The peak-to-peak torsional moment is predicted to be higher for the Mk3 blade whereas in the experiment it was reduced.

It is difficult to determine from the results presented whether the computer programs are capable of predicting accurately the effects

of large offsets between the centre of gravity and the shear centre. A larger difference in the type of response between the Mk2 and Mk3 blades would be needed to resolve this point. There is however one very important result from the testing of the Mk3 blade. The final stage in the design of the blade was to check experimentally the stability of the Mk3 blade compared with the Mk2 blade. Comparing these results with the predicted values showed that the stability program was accurate in the prediction that the Mk3 blade would be slightly less stable than the Mk2 blade. This supports the earlier observation in section 3.1 that the stability program predicts well the trends in blade stability. However, the Mk3(ii) prototype blade design, which included a new specification for the tip and the estimated structural properties presented in Table 12, had a flutter speed 21 rev/min above that for the Mk2 blade. This result shows that aft shear centre sweep will increase flutter speed.

5 AEROELASTIC TAILORING FOR A MODERN HELICOPTER

The results from the tests on the Mk2 model rotor have shown that aft tip sweep can reduce the flatwise bending and torsional moments on the inboard part of the blade in addition to providing a dynamic twisting of the blade which is beneficial to the aerodynamic performance of the rotor. The tests on the model blade were undertaken with a tip speed about half that found on full size rotor designs since the wind tunnel in which the model rotor was tested is limited in speed to 40m/s. The rotational speed of the rotor was reduced therefore to achieve advance ratios representative of full size helicopters.

The next step in the investigation of aeroelastic tailoring as a follow-up to the work presented in this thesis, will be to assess the performance and problems associated with flying a model rotor with a tip speed representative of a full size rotor. Future experimental tests at the RAE will take place in the 5m wind tunnel using a new rotor test rig and it would be a useful exercise, therefore, to investigate theoretically the effects of tip sweep on a full size rotor prior to the evaluation of designs for the new rig.

A suitable rotor configuration for the theoretical investigation is four bladed and articulated as for the proposed 5m tunnel rig. The rotor loads computer programs have been validated for dual load path hubs and in moving to a different hub design it would be desirable that flight test data were available to confirm that the theory is reasonably accurate in predicting at least the flatwise and torsional blade stresses. The EH101 blade could be a suitable candidate but, although the hub is articulated, it has five blades and extensive comparisons with theory have not been undertaken. The analysis of an aeroelastically tailored version of the EH101 blade could however be the basis of a useful exercise in the future. The only remaining helicopter rotor system that would be suitable and for which adequate flight test data is available is that of the Westland/Aerospatiale Puma with the standard blades.

The aim of the exercise was to examine the power, hub/blade loads and blade stability resulting from a hypothetical modification of the Puma blade of a similar type to that studied on the Mk2 swept tip blade as flown in the 24ft wind tunnel, and assess therefore if aeroelastic tailoring using swept tips is a practical proposition. The aim was to show the general effects of tip sweep and not to produce an optimised blade design.

5.1 The baseline Puma blade

The Puma helicopter has a four bladed articulated main rotor with a symmetric NACA0011 aerofoil section for the blade. The rotor radius is 7.5m and the blade chord is 0.537m. The theoretical model consists of the isolated main rotor with the shaft fixed at a 5° forward tilt as for the model rotor. The prediction programs are capable of dealing with a trimmed vehicle and can include the effects of the fuselage, but these have been omitted to simplify the problem. There is a large separation between the rotor and fuselage on the Puma so the fuselage upwash created in forward flight has a negligible effect on the rotor. The trim of the vehicle is very important when trying to predict the total power required. The evaluation of tip sweep on the model rotor (section 3.2) has shown that the cyclic and collective pitch is different for a rectangular blade and a swept tip blade. The pitch attitude of a vehicle with swept tip blades would therefore be different to the case where rectangular blades are used. A change in the pitch attitude would alter the drag of the fuselage and thus could obscure the effects of tip sweep on rotor power.

A sketch of the hub is shown in Fig 107. The flap and lag hinges attaching the blade to the hub are almost coincident, the lag hinge being nearest the hub. The pitch bearing is outboard of the two hinges and the pitch arm extends forward of the blade and attaches to the control rod at a point which is in line with the flap hinge axis. The hub includes also a lag damper although it cannot be represented in the prediction programs. The hub is simple to model unlike the DLP used in the model experiments and the J134 modes program is therefore used.

The structural properties outboard of the pitch bearing on the Puma blade are shown in Table 14. The flap and lag hinges are at 0.28m radius and the pitch arm extends 0.2032m in front of the pitch axis. The mass table shows that the mass/metre length of the tip is greater than that of the inboard section and the tip mass used to improve the autorotation performance of the helicopter is located at 7.4m radius. The blade twist is 7.36 degrees and the centre of gravity is behind the shear centre on the main section of blade and ahead of the shear centre at the tip. The overall centre of gravity is about 0.5% behind the shear centre.

Comparisons between the predicted bending and torsional moments and the moments measured on the inboard section of blade during flight trials have been made by C Young[53]. The results show that the predicted flatwise bending moments are in good agreement with the experimental values for advance ratios up to 0.3158 whereas the prediction of the torsional moments is good for low forward speeds and deteriorates at the higher speeds. The inability to model the lag damper and not being able to match the exact structural properties of the real blade could account for some of the errors in the predicted results. The accuracy of the prediction programs is considered adequate for the present exercise.

The mode shapes and frequencies for the Puma blade at the normal operating speed of 267.4 rev/min are shown in Fig 108. Comparing the mode shapes and frequencies with Fig 18 for the model rectangular blade, the four flap modes have similar non-dimensional frequencies whereas the torsional frequency is higher and the lag frequencies lower for the Puma blade. The second lag mode has also a lower frequency than the third flap and first torsional modes for the Puma blade. There is more coupling also between the flap and torsional motions of the Puma blade which is probably due to the offset between the centre of gravity and the shear centre - Table 14. A plot of the modal frequencies vs rotor speed is shown in Fig 109 for the Puma blade. The continuous lines represent the frequencies calculated using the J134 modes program and do not include aerodynamic damping. The points indicated by an 'x' were calculated using the stability

analysis and include aerodynamic damping. The good correlation between the two sets of results at the lower speeds indicates that the stability analysis should be able to pick out instabilities for the swept tip version of the Puma blade. Fig 110 shows the percentage critical damping for the modes presented in the previous figure apart from the first flap mode whose critical damping is above 50%. All of the modes presented have positive damping up to 21% overspeed indicating, as expected, a stable blade. There is some doubt, however, to the magnitude of the damping of the first torsional mode.

Fig 111 shows the predicted bending and torsional moments at 18% radius for advance ratios of 0.1 and 0.35. The maximum bending moments do not necessarily occur at the blade root of an articulated rotor and these results can only be used as a guide to the trend in the bending moments. There is a change in the waveform of the flatwise and edgewise bending moments as the advance ratio is increased. A similar result was observed on the model blade. The torsional moment is however largest at the blade root and the torque is reacted by the control system. The large control loads in high speed flight can limit the speed of the helicopter and it is important therefore to note the torsional moments whilst making modifications to the blade design. Fig 111 shows that the torsional moment increases by 80% as μ increases from 0.1 to 0.35.

Table 15 gives a summary of the performance of the baseline Puma blade for advance ratios up to 0.45. These values are used later to compare the performance of a swept tip blade with the baseline blade. Above $\mu = 0.35$ there is a sharp rise in the total power which is due to the high drag at the tip of the advancing blade. There is some dynamic twisting of the blade which arises from the shear centre - centre of gravity offset mentioned earlier, and from the aerofoil pitching moment which varies both with incidence and Mach number. The lower part of Table 15 shows the peak-to-peak blade root bending and torsional moments and the hub forces. The peak-to-peak flatwise and edgewise bending moments have doubled in magnitude between $\mu = 0.1$ and $\mu = 0.45$ whereas the torsional moment is 3.5 times larger. The hub

forces, which were calculated in the non-rotating global axes, generally decrease with increasing advance ratio for μ less than 0.3. Above $\mu = 0.3$ there is a rapid increase in the hub loads with advance ratio.

5.2 The Puma blade with tip sweep

The performance prediction programs and the stability analysis were used to explore the effects of tip sweep in conjunction with torsional stiffness and mass balancing on rotor performance. The general results of the modifications are shown in Table 16. All of the cases are at an advance ratio of 0.35 and at the same thrust as for the baseline blade results mentioned earlier. More detailed results are presented for the final case No 5.

The outer 10% of the rotor blade is swept back 20° without any modifications to the remaining structural properties for case No1. The main changes in the modal frequencies are in the third flap and first torsion modes which converge near the normal operating speed - Fig 109. The twist on the advancing side is increased relative to the baseline blade, as expected from the tests made on the model blade. The twist is decreased also on the retreating side of the disc possibly due to the additional flap/torsion coupling introduced by the tip sweep. There is a reduction in the Mach number normal to the blade tip on the advancing side of the rotor disc and there is a slight increase in rotor power. The flatwise bending moment is reduced but the edgewise bending moment and the torsional moment have increased. As mentioned earlier the torsional moment is particularly important as the torque is reacted by the control system. The sideways and vertical hub oscillatory forces are increased significantly. The stability program has shown the blade to be stable up to 25% overspeed with no sign of any reduction in the modal dampings immediately above 25% overspeed.

The second modification to the blade involved the use of the blade properties of the inboard portion of the blade over the whole of the blade. The tip mass used to increase the rotor inertia was removed

and the centre of gravity was positioned on the local shear centre. Most of the higher order modal frequencies are increased and apart from a reduction in power there is no significant benefit of the No2 design over the No1 design.

The next three modifications were intended to increase the dynamic twist on the retreating side of the rotor disc and decrease the twist on the advancing side. The torsional stiffness for blade No3 was reduced from 85000 Nm² to 55000 Nm². The modification improves the dynamic twist although the additional twist on the retreating side of the rotor disc was very low. The twist at $\psi = 270^\circ$ is increased by including a mass at the blade/tip junction as for the model blade - blade No4 and the reduction in the twist on the advancing side of the disc is restored by increasing the tip size to 12%R - blade No5.

Fig 112 shows the mode shapes and frequencies of blade No5. Comparing the mode shapes with those for the baseline blade, Fig 108, there is a significantly higher coupling between the flap and torsion motions of the blade. The main change in the modal frequencies is in the separation of the third flap and first torsion modes and the blade is stable at 25% overspeed. The rotor power is reduced slightly and larger reductions could be anticipated at higher forward speeds due to the beneficial dynamic twist and the tip sweep reducing the effective Mach number at the tip of the advancing blade. Although there are improvements in the aerodynamic performance, the blade root torsional moments have increased by 104% and the hub loads have increased relative to the baseline blade.

Fig 113 shows the blade root bending and torsional moments for the No5 blade. The change in the waveform of the torsion response is very similar to that observed for the model blade - Fig 38. On the advancing side of the rotor disc, $\psi = 90^\circ$, the tip sweep has produced a nose-up torsional moment and on the retreating side, $\psi = 270^\circ$, a nose-down moment. The difference between the model blade and the Puma blade is that the peak-to-peak torsional moment has been reduced on the model blade by introducing tip sweep whereas the torsion moment has been increased. The difference arises because the aerofoil used

for the model is cambered causing a large nose-down pitching moment on the advancing side of the rotor disc, whereas the Puma blade has a symmetric aerofoil which has a zero pitching moment coefficient at zero incidence.

5.3 The Puma blade with tip sweep and cambered aerofoil

The highly cambered aerofoil section chosen for this analysis, has an advantage over the symmetric aerofoil in that it can produce more lift at a given incidence and Mach number and the incidence at which pitching moment break occurs is higher for the cambered section at the lower Mach numbers. The disadvantage of this particular cambered aerofoil section is the high nose-down pitching moment which increases with Mach number and the high zero-lift drag coefficient (C_{D0}) although the incidence dependent drag can be lower than on a symmetric aerofoil at low Mach numbers. Another disadvantage of the cambered aerofoil is the poor performance at Mach numbers above 0.75.

The final stage in the theoretical analysis of an aeroelastically tailored full-size blade, was to replace the NACA0011 aerofoil section on the Puma blade with a modern cambered section. The mode shapes and frequencies were assumed to be the same as for the baseline blade and the performance was determined at an advance ratio of 0.35. Fig 114 shows the blade root bending and torsional moments on the Puma blade with a cambered aerofoil compared with the baseline blade. The rotor power is 753KW which is a rise of 40KW over the baseline blade at the same advance ratio. A more detailed examination of the results showed that the profile power had increased by about 50KW and the induced power had decreased by 10KW, suggesting that the overall power increase is due to the fact that the symmetric aerofoil has a thickness/chord ratio of 11% compared with a ratio of 12% for the cambered aerofoil. The aerodynamic performance of an alternative cambered aerofoil section with a 11% thickness/chord ratio was not available for this analysis. The flatwise and edgewise bending moments have increased significantly, but more important is the five fold increase in the torsional moment. The other performance parameters which have been addressed in the previous section are also worse for

the blade with the cambered aerofoil. The poor performance of this design is due to the high nose down pitching moment coefficient of the cambered aerofoil section. The aerofoil incidence at which pitching moment break occurs is also lower than for the symmetric aerofoil at high Mach numbers thus increasing the torsional moment at high forward speeds. The addition of a swept tip to this blade will not only reduce the local Mach number but will also hopefully reduce the magnitude of the advancing blade tip incidence. The final design analysed thus had the same structural properties as blade No5 used in the previous section with a 12%R tip and a cambered aerofoil for the main section of blade. The symmetric aerofoil NACA0011 was used for the swept tip to further reduce the high nose down pitching moment on the advancing side of the rotor disc and to reduce the drag through being a thinner aerofoil section.

Fig 115 shows the bending and torsional moments measured at the blade root for the baseline blade with a cambered aerofoil and the same blade with a swept tip. The flatwise bending moment is reduced and the edgewise bending moment has not altered significantly. The torsional moment is reduced to approximately the same level as found on the baseline Puma blade and the waveform of the torsion moment is similar to that found on the model swept tip blade. The rotor power is reduced from 753KW for the unswept cambered blade to 726KW, of which 7.6KW is due to the change in the swept tip from a cambered aerofoil section to NACA0011.

Table 17 shows the performance of the latest version of the Puma blade for advance ratios between 0.1 and 0.45. Comparing these results with those for the baseline blade, Table 15, the rotor power is on average only about 2% higher for the modified blade despite the fact that two blades with aerofoils of different thickness/chord ratios were compared in the analysis. The modified blade shows a higher level of twist both on the advancing and retreating sides of the rotor disc compared with the baseline blade which is due to the cambered aerofoil and the associated nose-down pitching moment. The twist of the modified blade on the advancing side of the rotor disc slowly increases with advance ratio up to $\mu = 0.4$. It can be assumed

from this result that the swept tip is not reducing the blade twist with advance ratio. The result should however be compared with that for a cambered aerofoil with no tip sweep to see that the swept tip is in fact reducing the twist with advance ratio. On the retreating side of the rotor disc the twist increases with advance ratio above $\mu = 0.3$. The twisting could be increased further by reducing the torsional stiffness of the blade or by increasing the size of the tip. The peak-to-peak flatwise bending moments are lower on the modified blade whereas the edgewise bending and torsional moments are higher. It is difficult to compare the peak-to-peak hub loads as there is a large variation with advance ratio. There is little difference overall between the two blades in terms of the hub loads except for the highest advance ratio calculated.

The results from the modification of a Puma rotor blade in a similar way to that of the model blade have shown that the main benefit of aft tip sweep is to reduce the rotor power. Better use of tip sweep can be made by using it to alleviate the large nose-down pitching moment produced by a more aerodynamically efficient cambered aerofoil. Care must however be taken to select a suitable aerofoil for the tip section to avoid the very high pitching moment associated with cambered aerofoils operating at high Mach numbers. A very important result from the theoretical analysis of the Puma blade combined with reduced torsional stiffness is that aft tip sweep will not necessarily give rise to some form of rotor instability near or below the normal rotational speed.

6 DISCUSSION AND CONCLUSIONS

6.1 Discussion and future work

A rotor blade has been designed to investigate the effects of sweeping back a significant portion of the blade tip and accepting a large coupling between the lift at the tip and the torsional response. The aim of the first design was to validate the design tools and prediction methods, to demonstrate the possible benefits of swept tips and to show any possible problems associated with designing and flying a swept tip blade. An initial design suffered from a stability problem and was redesigned with the aid of a new modes/stability analysis. The stability analysis was a useful tool in identifying the nature of the instability and for finding a solution. The analysis was complemented by a number of hover tests made on the unstable blade with various amounts of mass balancing and increased torsional stiffness. The results from these tests proved to be very useful during the further development of the stability analysis at the RAE. The redesigned blade had slightly different modal frequencies to the baseline rectangular blade due to the increased torsional stiffness and additional mass balancing, but was considered to have only a small effect on comparisons made between the blades with and without tip sweep.

Comparisons between the measured moments on the baseline blade and on the new Mk2 swept tip blade showed that a beneficial azimuthal variation of blade twist had been achieved, whilst keeping the bending and torsional moments well within the prescribed limits. The swept tip had reduced the nose-down pitching moment on the advancing side of the rotor disc and hence the peak-to-peak torsional moments were lower than those on the rectangular blade at high advance ratios. Further modifications to the blade design will probably reduce further the bending and torsional moments especially at the lower forward speeds.

The experimental results were compared also with those predicted by the RAE/VHL coupled modes analysis. The correlations were fairly good for the flatwise bending and torsional moments at the low advance ratios. The calculated torsional moments were in poor

agreement at the high advance ratios especially on the advancing side of the rotor disc. There was evidence of the second nose-down peak on the retreating side of the disc although it occurred at a larger azimuth angle.

A change in the modelling of the hub to represent more accurately the two load paths showed that a significantly better prediction of the torsional moments could be achieved without affecting the accuracy of the predicted flatwise bending moments. The accurate prediction of the flatwise bending and torsional moments is particularly important whilst investigating aeroelastic rotors. Further work is however necessary to resolve the discrepancies in the edgewise bending moments. Experimental work is planned to measure the non-rotating blade mode shapes and the rotating modal frequencies. The results will be very useful in identifying errors either in the prediction programs or in the way the rotor has been modelled. The technique known as Strain Pattern Analysis (SPA) [54] could be a useful way of determining the rotating mode shapes although a larger number of strain gauges is required especially near the blade root and on the hub. The prediction methods were considered to be sufficiently accurate for design purposes, although areas for improvement are identified in the rotor loads prediction program. The prediction methods were used to aid the design of blades incorporating further features of the aeroelastically tailored concept.

An investigation of the effects of aft tip sweep on the performance of the model rotor with increased blade twist showed that the blades root stresses were lower for the higher twist but at the expense of blade stability. For a rectangular blade, an increase in blade twist usually leads to an increase in blade stresses.

A parametric study was undertaken to investigate the effects of linear sweep back of the shear centre on the main section of blade. The results showed that for increasing sweep, the peak-to-peak flatwise and torsional blade moments decreased and the stability margin increased. The maximum sweep angle attained was 1.1° prior to

an instability of the performance program. For a 1.1° aft sweep the convergence of the highly coupled 3F/1T mode and the 3F mode occurred at 690 rev/min compared 660 rev/min with no shear centre sweep.

The aft shift in the shear centre was achieved by reducing the width of the leading edge layers of glass fibre towards the blade tip. The reduction in the layers meant that the flatwise and edgewise stiffnesses also decreased so additional layers with an inverse taper were introduced to the trailing edge. The rearward shift in the shear centre was coupled with a shift in the centre of gravity and the additional weight needed for mass balancing reduced the stability margin. An alternative design with symmetric $\pm 45^\circ$ carbon fibre layers could have been used as a more efficient, i.e. lighter, way of sweeping back the shear centre and thus avoiding the additional mass and increasing the stability margin. However, the use of carbon fibre could have introduced high local stresses between the carbon fibre and the weaker glass fibre fairing thus leading to a complete redesign of the blade structure.

The results from the wind tunnel tests on the Mk3 blade showed that the blade responses were very similar to those for the Mk2 blade and it was therefore difficult to determine the level of accuracy of the theoretical analysis in predicting the bending and torsional moments. The most important result from the analysis using the stability program is that linear aft shear centre sweep increases the stability margin.

A theoretical analysis of the use of tip sweep on a full size Puma rotor was made. The radial extent of tip sweep and the sweep angle depend on the amount of dynamic twist required and the torsional stiffness of the blade. In the analysis of the Puma blade there was no optimisation of the blade design as the aim was to identify the main advantages and disadvantages of tip sweep on a full size rotor. The results from the exercise showed that the modification was of little benefit to a blade with a symmetric aerofoil section. There were improvements to the rotor performance in terms of reduced power, but they arose mainly from the sweep effects and not from the modified

aeroelastic response. There were increases however in the blade root torsional moment and hub vibration. Larger gains could be observed if the swept tip was added to a blade which had a cambered aerofoil section. The large nose-down pitching moment associated with the particular cambered aerofoil chosen could be greatly reduced on the advancing side of the rotor disc and the aerodynamic benefits of the new aerofoil over a symmetric section could be used to improve the rotor performance. Unlike the model rotor the Puma blade was stable up to 25% overspeed with an aft swept tip and no additional chordwise mass balancing.

The next step in the investigation of aeroelastic tailoring at the RAE is to fly a model rotor blade with a tip Mach number representative of full scale rotors. The theoretical analysis on the Puma blade has been a useful exercise in identifying the areas where further work is required. Such areas include the selection of a suitable aerofoil section for the tip region to avoid a premature pitching moment break on the advancing side of the rotor disc and the need to reduce the hub and control system loads.

The data collected during the wind tunnel tests on the model swept tip blades will be very useful for the validation of the next generation of blade loads prediction programs. One such program is being developed jointly by RAE and Westland Helicopters Ltd [55] which includes multiblade degrees of freedom coupled with the fuselage dynamics. The hub will no longer have to be considered as rigid and the aeroelastic response of one blade will directly affect the response of the remaining blades.

6.2 Conclusions

A swept tip helicopter blade has been designed at model scale for testing in the RAE 24 ft wind tunnel. An initial design suffered from a stability problem and was redesigned with the aid of a new modes/stability analysis and hover tests. The stability analysis was a useful tool in identifying the nature of the instability and for finding a solution.

Comparisons were made between the stresses measured on a second, stable, swept tip blade and a dynamically similar rectangular blade. The results show that a beneficial twisting of the swept tip blade was achieved to enhance rotor performance. Flatwise bending and torsional moments were also reduced.

A comparison was made between the blade bending and torsional moments predicted by the RAE/VHL coupled modes program with those obtained experimentally. The results show that the analysis is reasonably accurate for a large section of the flight envelope. A further improvement in the prediction of the torsional moments at high advance ratio was achieved by using a better representation of the hub in the computer model.

A further set of blades has been designed to investigate the effects of aft sweep of the shear centre. The sweep introduced a strong coupling between the flap and torsion motions of the blade. An analysis of the results showed that there were possible gains in blade stability with no adverse effects on blade loads.

A theoretical investigation of the performance of a full size rotor with aft tip sweep showed that control loads and rotor power could be reduced for blades with a cambered aerofoil section.

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APPENDIX A

THE BLADE DESIGN TOOLS

The analytical methods used in the design of rotor blades are the modes calculation[A1] and rotor loads analysis developed by the RAE and WEL[38]. Fig A1 shows how these programs fit into the design process. The structural data are essentially lists of blade properties such as the flatwise, edgewise and torsional stiffnesses, chordwise offsets between the shear centre and the centre of gravity, inertias, etc. The data includes also the geometry of the rotor and information regarding any additional load paths which are represented by systems of linear and rotational springs to earth. The properties for the hub components can generally be calculated easily as they are made from a single isentropic material. The blade properties, however, are more difficult to determine due to the relatively more complex construction, the non-symmetry of the cross-section and the use of materials such as carbon and Kevlar fibres and Rohacell foam. The properties were therefore determined by a combination of experimental methods[A2] and modelling using finite element techniques[A3]. The structural data is used by the modes program (known as J134) to calculate the coupled mode shapes of a rotating blade. The J134 program uses a modal approach to solve the dynamic equations rather than a finite element technique. The method is applicable to a blade of very general geometry although the amount of validation performed for swept tip blades is small. The geometry of the blade is described by using the global coordinates of the shear centre locus. The geometry has to include precone and twist and it is particularly important to take account of these when calculating the coordinates for a blade with a swept tip. The blade properties are then defined at various points along the shear centre locus. The program treats a single blade mounted on a stiff hub so that the motion of one blade is not affected by the dynamic response of the other blades. Steady state loads can be included in the analysis and therefore the displacement of the modes in the solution of the forced response equation, will be about the steady state position of the blade. Section 2.2 describes the modelling of the dual load path hub and the rotor blade in more detail.

The modal analysis provides any number of mode shapes and natural frequencies allowing the forced response to be calculated using modal coordinates. The computer program used to calculate the forced response, which is known as R150, includes the calculation of the aerodynamic forcing, time dependent terms such as Coriolis force, terms dependent on the variation of blade pitch and non-linear terms not included in the modal analysis. Only a maximum of eight of the modes calculated by J134 can be used. The presence of all the blades is particularly important in this part of the analysis. The wake which includes the strong tip vortex shed from one blade, has a significant effect on the lift produced by the remaining blades as a result of its induced velocity on the blades. The wake is divided into two sections; the near wake and the far wake. The near wake comprises of a number of semicircular vortex elements shed from each blade. The strength of each vortex is related to the lift at the point of the blade from which it is shed. Since the blade lift varies as it moves in azimuth, the vortex strengths vary also depending on the azimuth point at which the induced velocity is calculated. The far wake is composed of circular vortex elements originating from only the root and tip of each blade. In this case the strength of these vortices depends on the mean loading on the blade.

The performance program uses a data base of aerofoil properties which have been obtained from wind tunnel tests. The rotor blade can be made up of up to eight aerofoil sections and linear changes from one section to another can also be included. A number of options exist as to the types of modelling used. For example, there are three types of downwash model i.e. Glauert, vortex ring or vortex ring with interactive near wake. The simpler Glauert model is mainly used for overall performance calculations as the greater accuracy of the vortex ring model is not required. Unsteady aerodynamic effects can also be included and there is a model for attached flow and a choice of a time delay model or a leading edge velocity criterion for dynamic stall.

A rotor trim routine in the performance program alters the blade cyclic and collective pitch to attain the desired flight conditions. The blade collective pitch will however also affect the

blade mode shapes. From past experience, the angle used in the modes program is valid if it is within 3° of the angle calculated in the trim routine.

The output from the performance program gives the azimuthal variation of the modal displacements. These are used together with the mode shapes to calculate the blade and hub loads by modal summation.

The quantity of data created by these programs is very large and to analyse the loads along the blade and hub with certain other parameters, such as geometric incidence and stall parameters, extensive use of plotting programs is made. One such package, DATAMAP[31], is capable of collating various data sets and presenting the results in a number of ways. The package was written specifically for analysing the results obtained from rotor tests and it includes therefore a number of useful facilities such as harmonic analyses and data filters. The latter is particularly useful in analysing experimental data as excessive noise on some strain gauge channels can be eliminated.

Simpler plotting packages are required also in some cases such as parametric studies. These programs were written by the Author to permit faster comparisons of predicted results on two blade configurations such as a baseline blade and a new design.

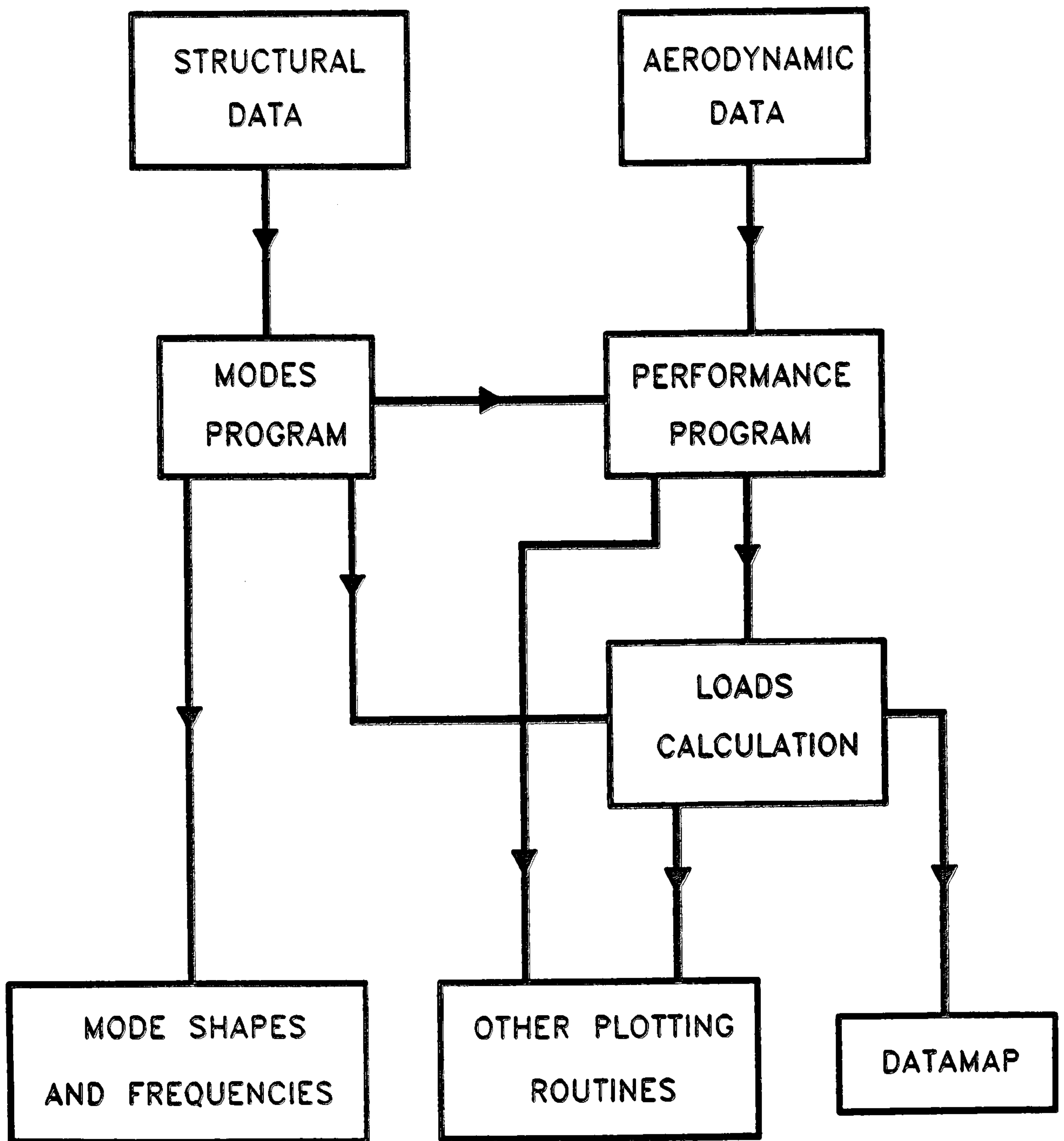


Fig A1 Computer prediction programs used in helicopter rotor blade design .

APPENDIX B

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THEORETICAL AND EXPERIMENTAL ANALYSIS OF A MODEL ROTOR
BLADE INCORPORATING A SWEEP TIP

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Abstract - The design of a swept tip model rotor blade for testing in the RAE 24ft Wind Tunnel is described. The swept tip is chosen to provide a pitch-flap coupling and an azimuthal variation of blade twist. The design is the first step in an investigation of aeroelastic tailoring as a means of reducing helicopter vibration and increasing rotor performance.

Comparisons are made between the stresses measured on the swept-tip blade and those on a dynamically similar rectangular blade. Comparisons are also made between the experimental results and those predicted by the RAE/WHL coupled modes analysis and an assessment is made of the suitability of the method for the design of a further set of blades. The aim of the second design is to achieve a performance improvement such as reduced vibration whilst allowing the blade to vary its twist with azimuth.

1. INTRODUCTION

The vibrational levels of helicopters are currently much higher than those of fixed wing aircraft throughout the flight envelope but especially when operating at high thrust or high forward speed. This leads to pilot fatigue and reduces the life of many helicopter components. The alleviation of vibration has attracted much attention for many years with several different approaches being adopted. These include passive devices such as absorbers, active systems such as higher harmonic control, and active control of structural response, all of which carry a weight penalty. A more beneficial solution may be to reduce the dynamic loads at source through the design of blades that generate much lower levels of vibration, but this requires a better understanding of the influence of blade aerodynamic and elastic design on oscillatory loads. However, a clear demonstration of the fact that blade design can have a major impact on vibration has been provided by the flight tests of the BERP rotor by Westland Helicopters Ltd (WHL) (1).

Another limitation of a rotary winged vehicle compared to an aeroplane is its inability to attain high forward speeds. Speed is limited by retreating blade stall which causes large oscillatory loads in the control system and high blade stresses. An increase in rotor speed delays the onset of retreating blade stall but introduces adverse effects on the advancing side of the disc. The advancing blade tip experiences transonic flow at high forward speed leading to increases in blade drag, vibration and noise. Other rotorcraft configurations such as compound helicopters and tilt rotor aircraft can achieve higher forward speeds but at the expense of a reduced payload fraction compared to a conventional helicopter. An improvement can be achieved by considering blades with complex tip shapes. A measure of the success of this approach is the recent world speed record attained by the Westland Lynx (2) fitted with BERP 3 blades. These blades use advanced aerofoil sections and a swept tip. The tip reduces Mach number effects on the advancing side of the rotor disc and also generates strong vortices to delay stall on the retreating side.

The choice of the blade parameters such as the twist is a compromise between hover and forward flight performance. High twist, which is beneficial in hover, is a disadvantage at high speed since the blade tip on the advancing side of the disc operates at a negative incidence. The inboard portion of the blade however is at a positive incidence which leads to high flapwise stresses in addition to any adverse effects arising from transonic flow phenomena. The amount of twist therefore needs to be reduced on the advancing side of the disc as the forward speed of the helicopter increases whilst retaining high twist on the retreating side.

An azimuthal variation of blade twist can be achieved by introducing offsets between the aerodynamic centre, the torsion axis and the section centre of gravity or by having a structural coupling between two blade motions such as flap and torsion. The so called aeroelastically adaptive rotors are capable of not only redistributing the radial and azimuthal lift but can also reduce vibratory loads (3). The investigation of this rotor concept is not easy because of the large number of possible parameter variations. A simple swept tip with the aerodynamic centre aft of the flexural axis of the inboard section is therefore considered as the first step. A theoretical study of a rotor blade incorporating a swept tip (4) has shown that tip sweep has a large influence on blade dynamic behaviour. There is also the possibility of modal frequency coalescence which in some cases can lead to flutter.

The aim of the study is to develop an understanding of the dynamic response of a rotor with a swept tip designed deliberately to introduce some of the couplings mentioned above, which were avoided in the BERP design. The aerodynamic benefits of the tip cannot be investigated because of the limited forward speed capability of the Royal Aerospace Establishment (RAE) 24ft Wind Tunnel in which the rotor is tested. The investigation however demonstrates the possible benefits as well as showing the problems associated with designing and flying a swept-tip blade.

The study is divided into two phases. The first part, which is described in this paper, involves the design and testing of a swept-tip blade with the main aim of validating the design tools and prediction methods. Comparisons are also made against a dynamically similar baseline rectangular blade. Performance benefits are not sought in this initial phase. Once confidence in the analytical methods has been established, the second phase will involve the design and testing of blades with particular improvements in dynamic response.

2. DESIGN OF THE SWEEP-TIP BLADE

The design of the swept-tip blade has been described elsewhere [5] and only a summary is presented below.

The construction of the rotor blade, Fig 1, utilises an existing blade mould and is limited therefore in the possible variations of blade geometry. The mould has a RAE 9642 aerofoil section, a chord length of 0.14 m and a $4.4^\circ/\text{m}$ twist. The tip section is untwisted, has a 20° sweep and a chord length of 0.14 m measured perpendicular to the leading-edge. The rotor radius measured from the centre of rotation to the tip taking into account the 2° pre-sweep and the tip sweep is 1.8 m, the same as the baseline rectangular blade [6,7]. The three-bladed rotor is mounted on the dual load path hub, Figs 2 to 4, used in previous experiments in the RAE 24ft Wind Tunnel [8,9].

The first stage in the design of the swept-tip blade was to determine the spanwise extent of the tip, R_T , Fig 1. The tip had to provide sufficient torque to twist the blade but the bending moments and loads could not exceed those previously determined by static and fatigue tests.

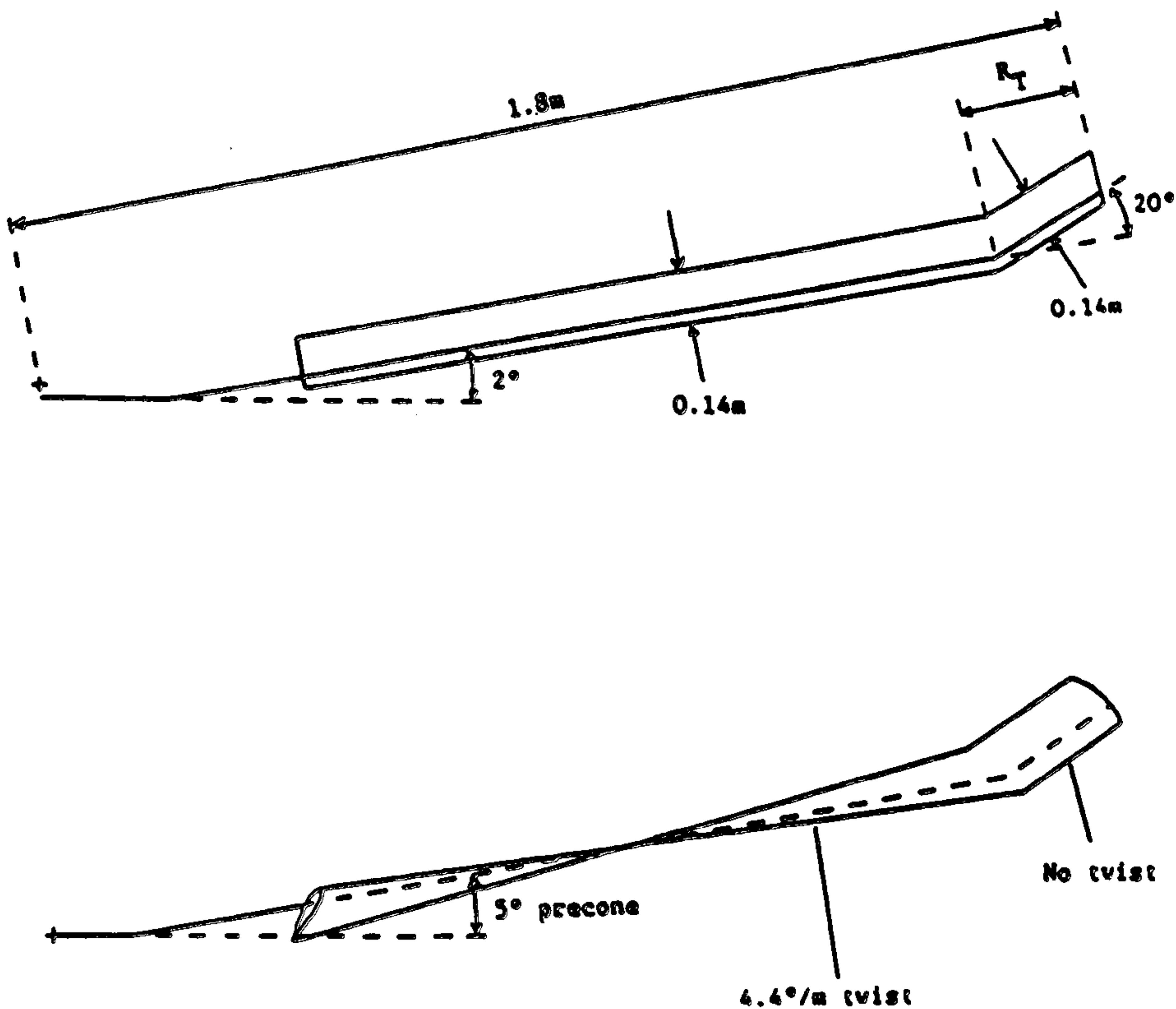


Fig 1. Swept-tip blade geometry

The analytical methods used in the design of the blades are the modes calculation and the rotor loads analysis developed by the RAE and WNL. The methods are applicable to a blade of very general geometry although the amount of validation performed for swept-tip blades is small.

A range of blade designs was considered with the tip size varying between 8% and 20% radius, and from those, the blade with a swept tip over the outer 12% of the radius provided an adequate once-per-rev twisting of the blade and the blade and hub loads were at acceptable levels. Because part of phase one of the study was a comparison between the swept-tip blade and the straight baseline blade, some refinement to the swept-tip blade design was necessary to provide as close a match as possible of the modal frequencies and structural properties to those of the baseline blade.

A new modes calculation and stability analysis [10] became available in time for investigating the swept-tip blade. The new method, developed at RAE, calculates the blade modes with applied aerodynamic loads and then performs a stability analysis. The modes are complex because aerodynamic, and optionally, structural dampings are included.

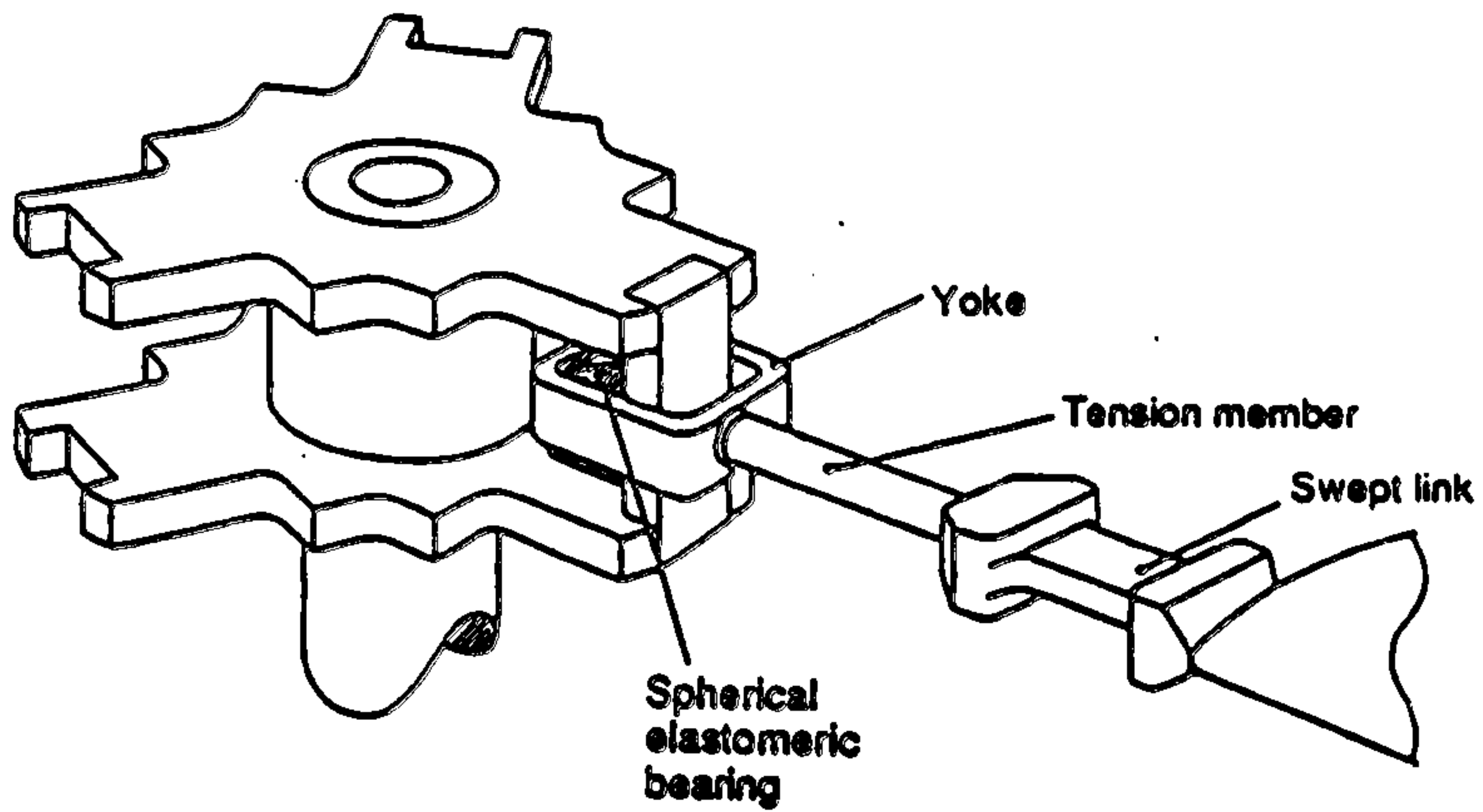


Fig 2. Hub assembly - centrifugal load carrying elements

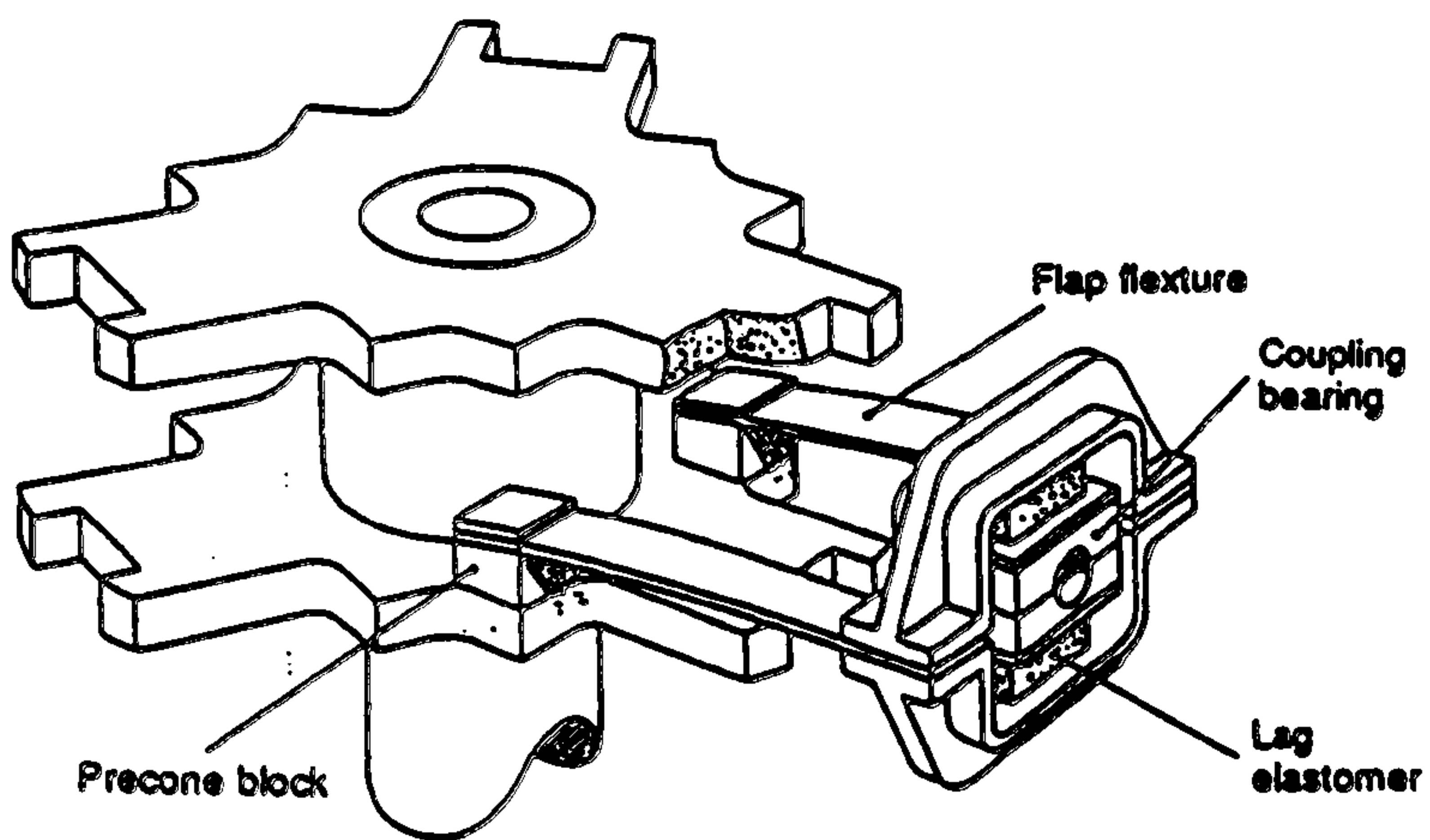


Fig 3. Hub assembly - flap and lag flexure elements

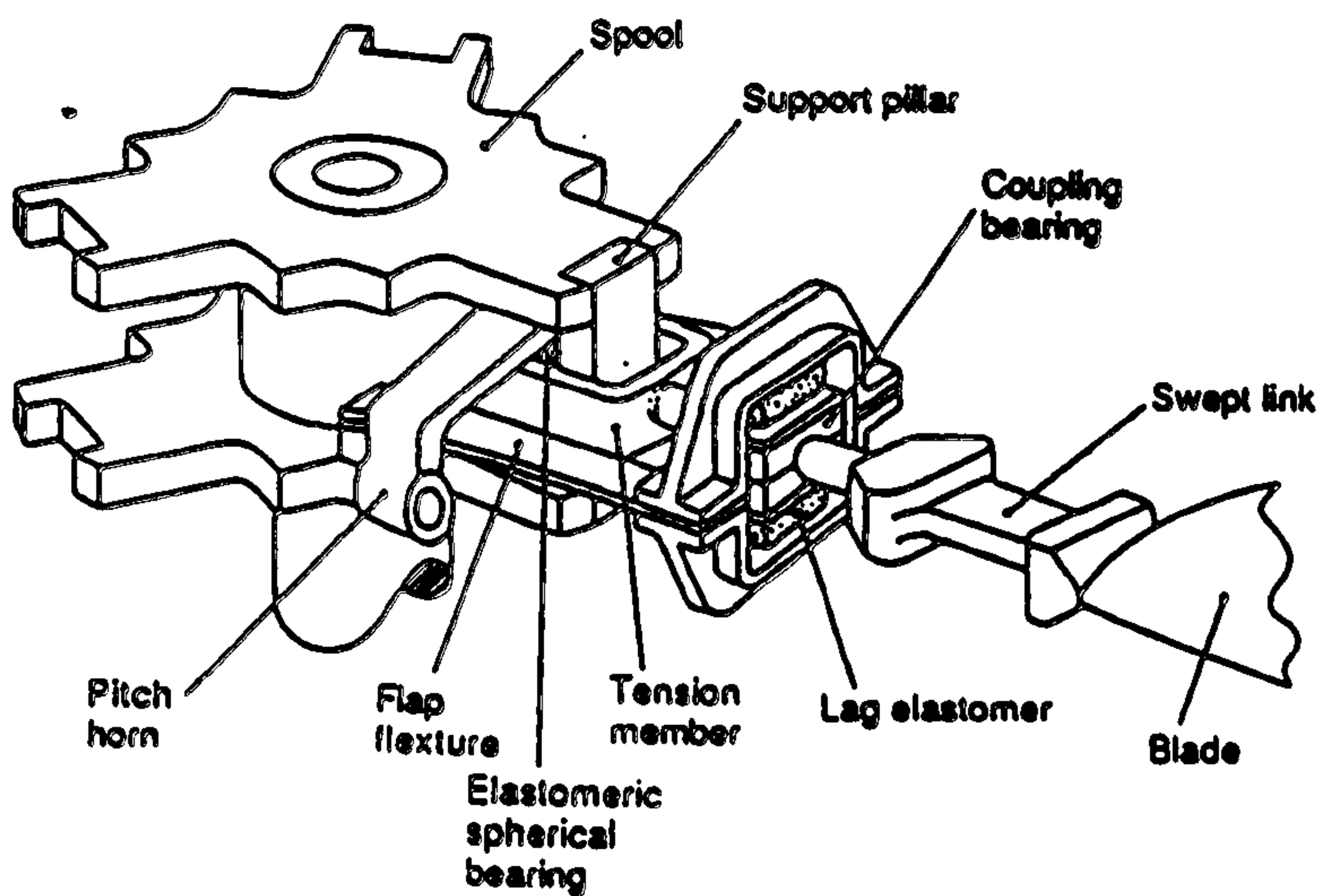


Fig 4. Complete hub assembly

Calculations indicated an instability at 574 rev/min and showed that the problem was due to the coalescence of the third and fourth modes. The third mode was the second flap mode and the fourth the highly coupled third flap/first torsion mode. A spoke diagram of the damped modal frequencies showing the two modes converging is presented in Fig 5 along with the calculated critical damping, expressed as a percentage, for the two modes. This result gave some confidence in the calculation method although experiments were still thought necessary in view of the unproven nature of the analysis.

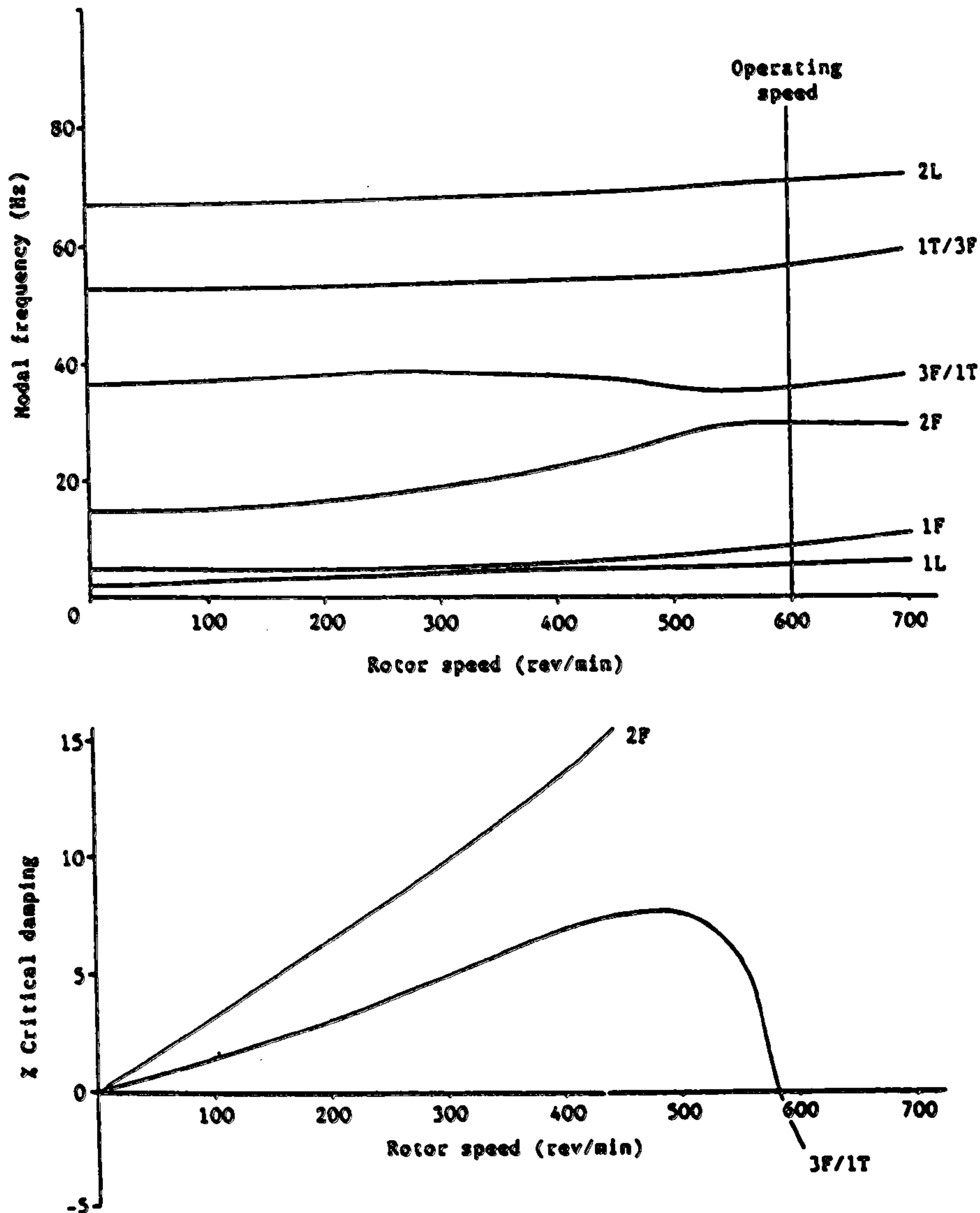


Fig 5. Predicted modal frequencies and dampings for the swept-tip blade

The standard method for alleviating flutter for fixed wing aircraft is to increase the torsional stiffness of the wing or introduce extra chordwise mass balancing. Both of these remedies were explored using the new modes and stability program. The torsional stiffness of the main section of blade was increased by 10% but this was found to be less effective than the addition of a mass, equivalent to 10% of the blade mass, on the leading edge at the blade/tip junction. The results also showed that moving the mass away from the tip junction nearer the blade root reduced its effectiveness presumably because it was nearer to the nodes of the second and third flap modes. Moving the mass outboard on to the swept tip reduced the mass balancing and made it more difficult to decrease the amount of coupling in the modes.

A series of tests [11] was also made on blades with increased torsional stiffness and additional mass added to a 0.1 m length of the leading edge at the blade/tip junction. The torsional stiffnesses tested were 56, 68, and 75 Nm², 56 Nm² being the stiffness of the swept-tip blade, with masses equivalent to 5, 10, 15 and 20% of the blade mass. The flutter speed of the various combinations is plotted against the leading-edge mass in Fig 6. The arrows on two of the points indicate that the test was terminated due to large oscillatory motions of the tower and not due to blade instability.

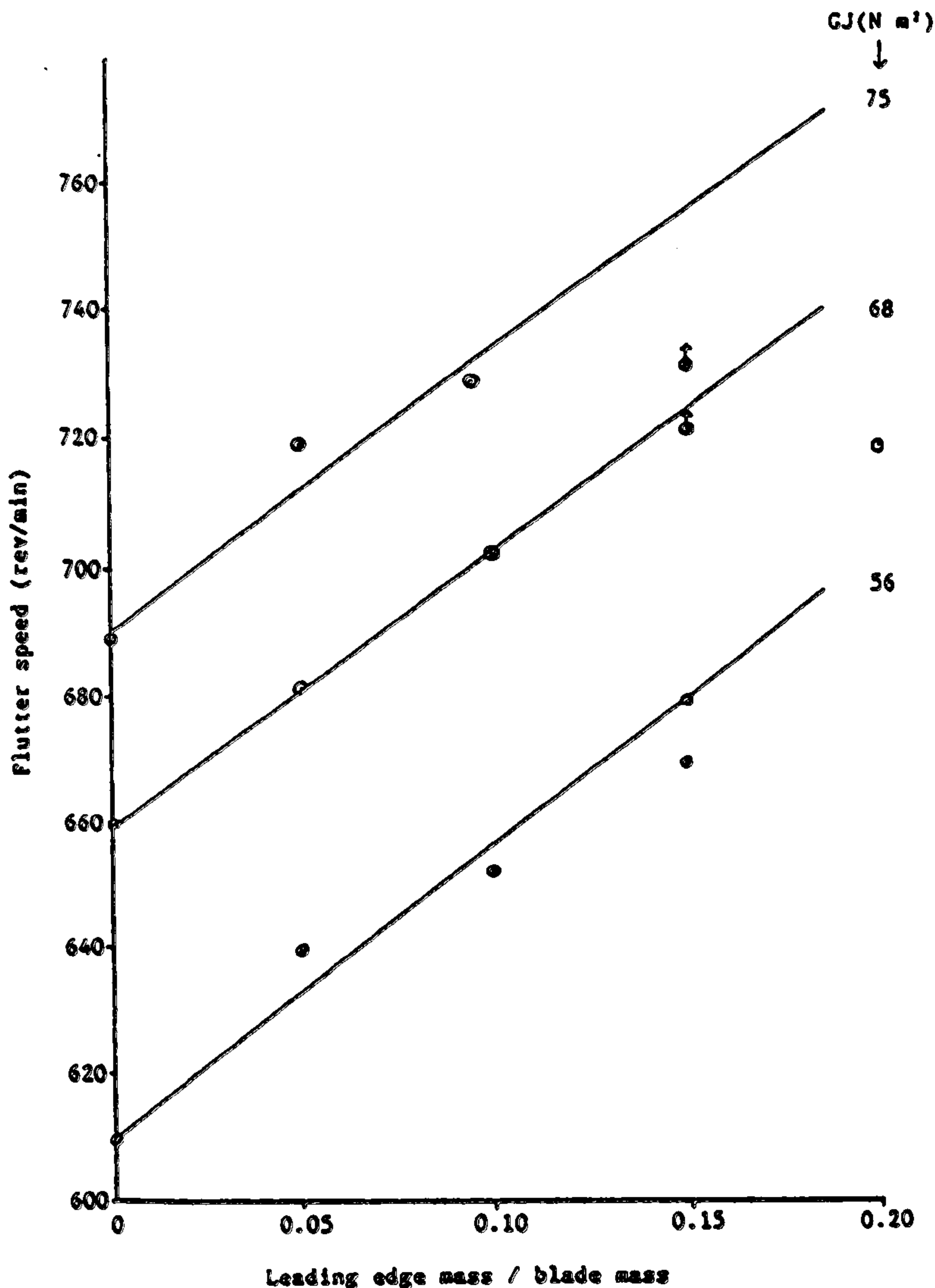


Fig 6. Effect of mass and torsional stiffness on flutter speed

Three bounds can be placed on the combinations of torsional stiffness and mass when selecting a suitable design. The absolute minimum flutter speed required is 680 rev/min. The second bound limits the additional mass to no more than 10% of the blade mass since greater masses produce unacceptable torsion loads at the tip junction. Finally, torsional stiffnesses greater than 75 Nm² increase the fundamental torsion frequency too much to enable a valid comparison to be made with the straight baseline blade.

The compromise selected for the blade is a torsional stiffness of 68 Nm² and a leading-edge mass equivalent to 10% of the blade mass distributed over 0.1 m at the blade/tip junction. A plot of the modal frequency vs rotor speed for both the swept-tip blade and for the baseline rectangular blade is shown in Fig 7. The modal frequencies have been calculated without allowance for any structural or aerodynamic damping. The most noticeable differences in the modal frequencies are the relative positions of the third flap and the first torsion modes although there are problems in resolving the nature of the fourth and fifth modes for the swept-tip blade. Comparing the mode shapes for the baseline blade, Fig 8, with those calculated for the swept-tip blade, Fig 9, shows that the swept-tip blade has larger couplings between flap and torsion in the higher order modes.

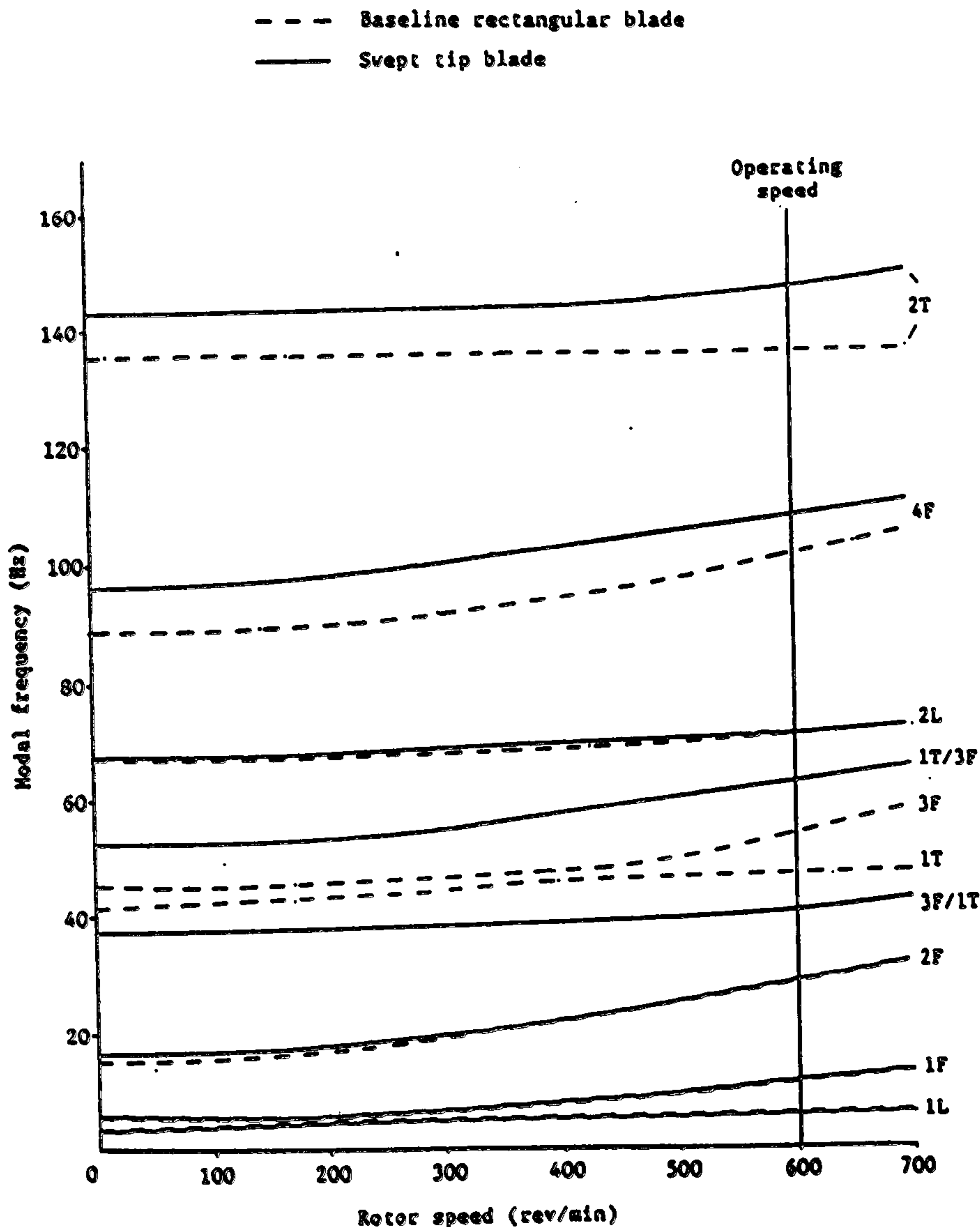


Fig 7. Comparison of the modal frequencies for the rectangular blade with those for the swept-tip blade

The blades were built as specified and tested on the wind tunnel tower. The flutter speed was 9 rev/min lower than the expected speed obtained when extrapolating from the laboratory stand results. The difference may be due to the position of the additional balance weight which is installed behind the existing leading-edge weight instead of attached to the blade surface. The flutter speed is acceptable, but below the originally desired 20% overspeed criterion of 720 rev/min, so care is needed when flying the rotor in the wind tunnel. Rotor speed is not automatically controlled in the tunnel tests and speed changes as collective pitch is altered so attention is necessary when reducing collective pitch from a high thrust setting.

The instrumentation of the rotor consists of strain gauges measuring flatwise, edgewise and torsion strains at ten spanwise stations on one of the blades. The radial positions of the eight inboard gauges correspond to those on the baseline blade. The two other blades have only one strain gauge at 30% radius. Measurements are also made on the swept link, flap flexure and on the pitch link which connects the pitch horn, Fig 4, to the rotor control system.

3. WIND TUNNEL TEST RESULTS

Comparisons between the swept-tip blade and the baseline-rectangular blade are made in this section. The flight cases considered are advance ratios between 0.1 and 0.34 at 900 N thrust which is equivalent to thrust coefficient/solidity of 0.0638. All of the test data are filtered using a cut off frequency of 300 Hz to remove the noise from some of the signals. Also, the dc component of the signal has been removed as the datum is different for the two rotors. Comparisons have only been presented for an inboard blade station although similar results are found further outboard.

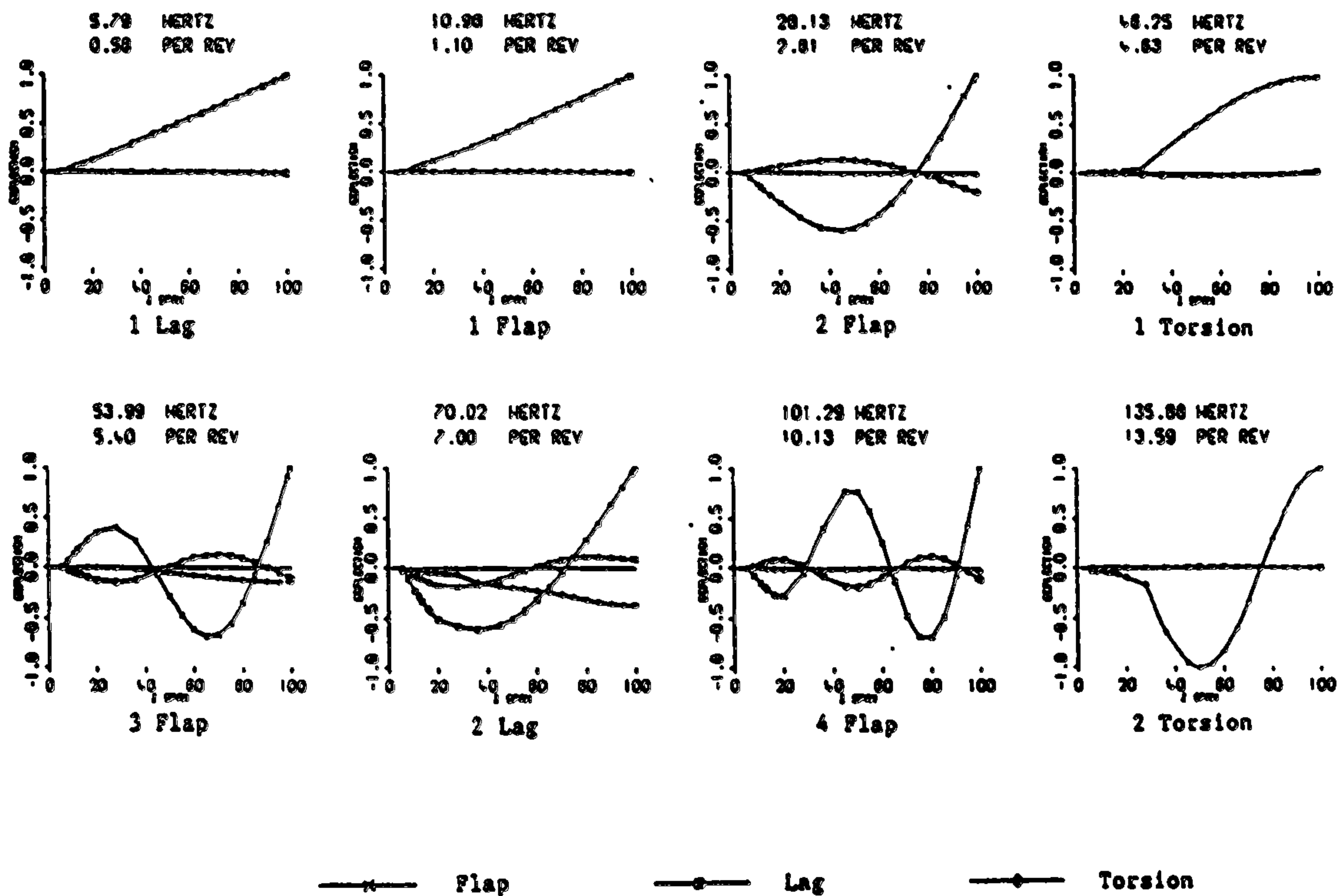


Fig 8. Mode shapes for the baseline rectangular blade

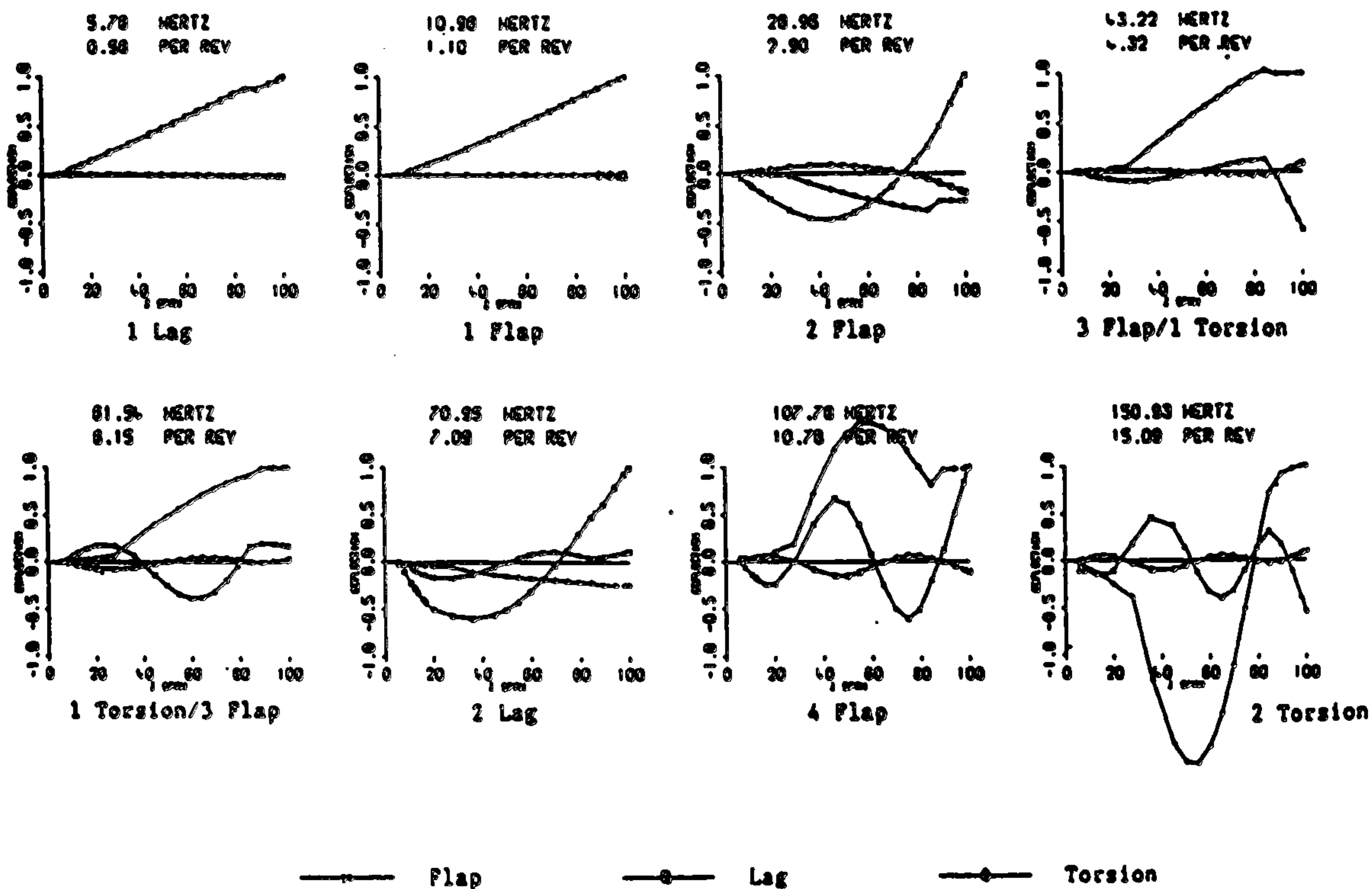


Fig 9. Mode shapes for the swept-tip blade

Fig 10 shows the azimuthal variation in the blade moments at an advance ratio (μ) of 0.1. The top two graphs show the flatwise and edgewise moments respectively. The azimuthal moment variations are very similar for the two blades except for a higher 3R component in the edgewise response of the baseline blade. The bottom plot shows the variation in the torsional moment. A harmonic analysis reveals that the swept-tip blade has equally large 1R, 2R, 3R, and 4R components compared with a mainly 1R signal on the baseline blade. The peak-to-peak moment is also much larger on the swept-tip blade. The flatwise and edgewise moments are again similar for both blades at an advance ratio of 0.34, Fig 11. The most noticeable difference between the torsion moments for the two blades is the additional nose-down moment for the swept-tip blade on the retreating side of the rotor disc. On this side of the disc the blade is at a large positive incidence and hence the lift from the tip whose centre of pressure is located aft of the shear centre of the main section of blade, forces the blade to twist nose-down.

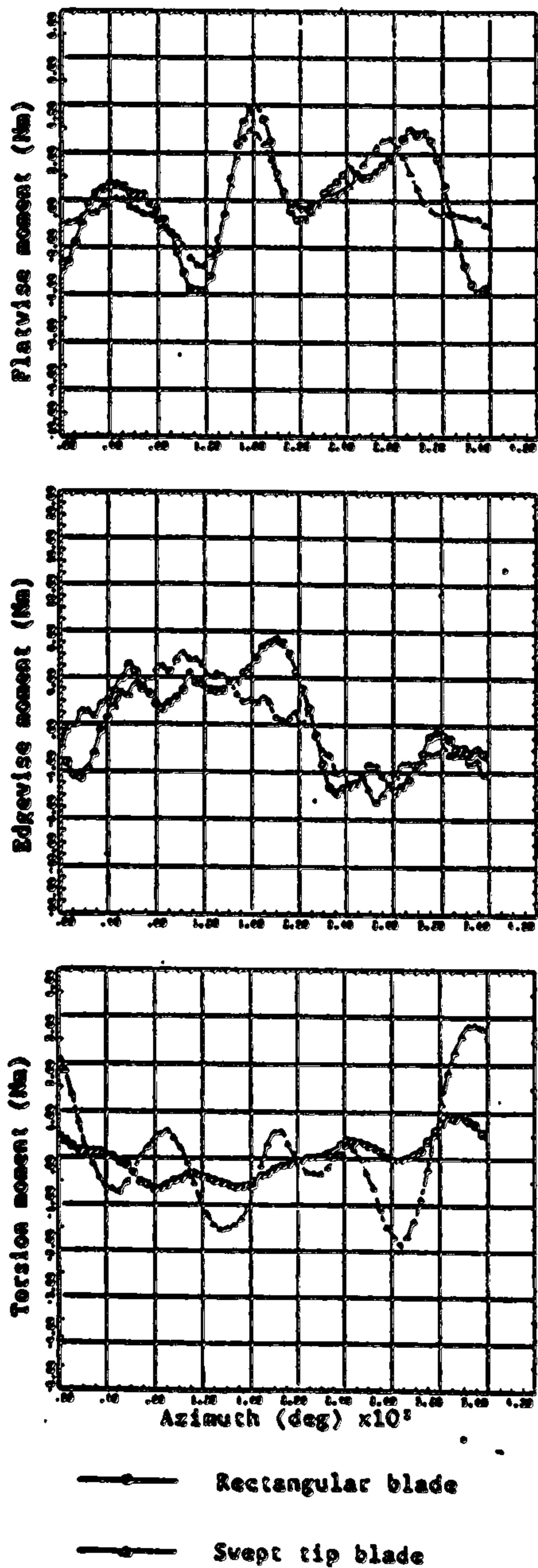


Fig 10. Measured blade root moments on the rectangular and swept-tip blades, thrust = 900N, $\mu = 0.1$

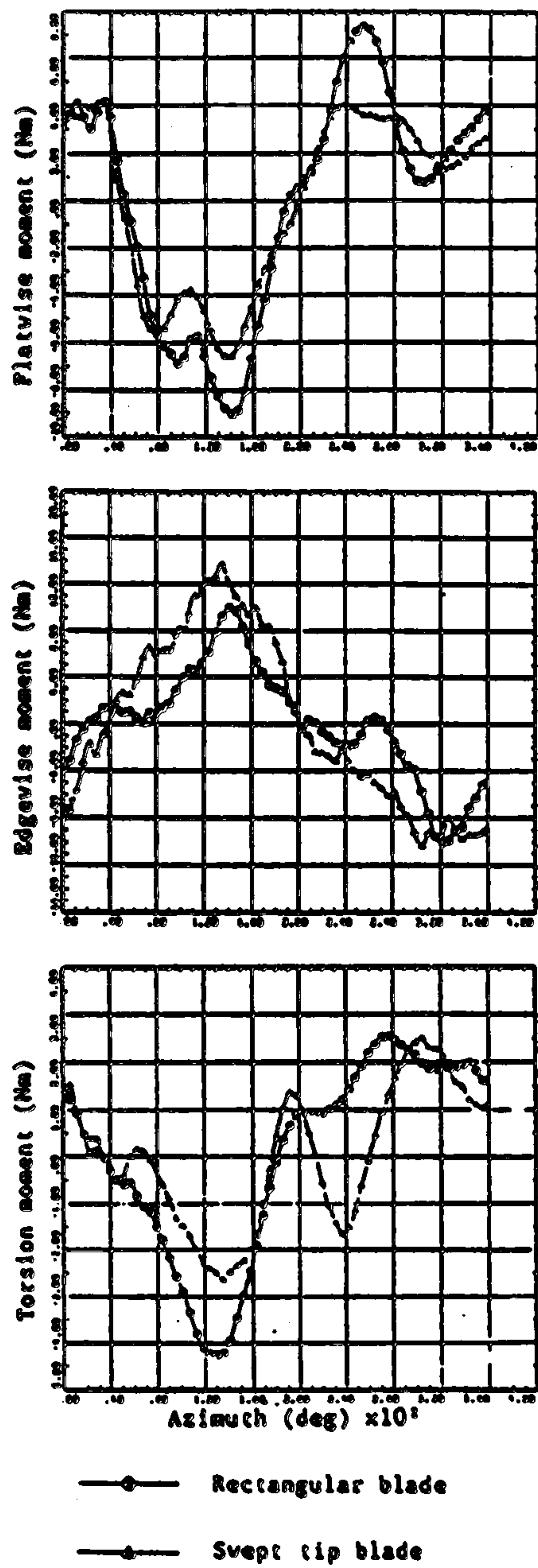
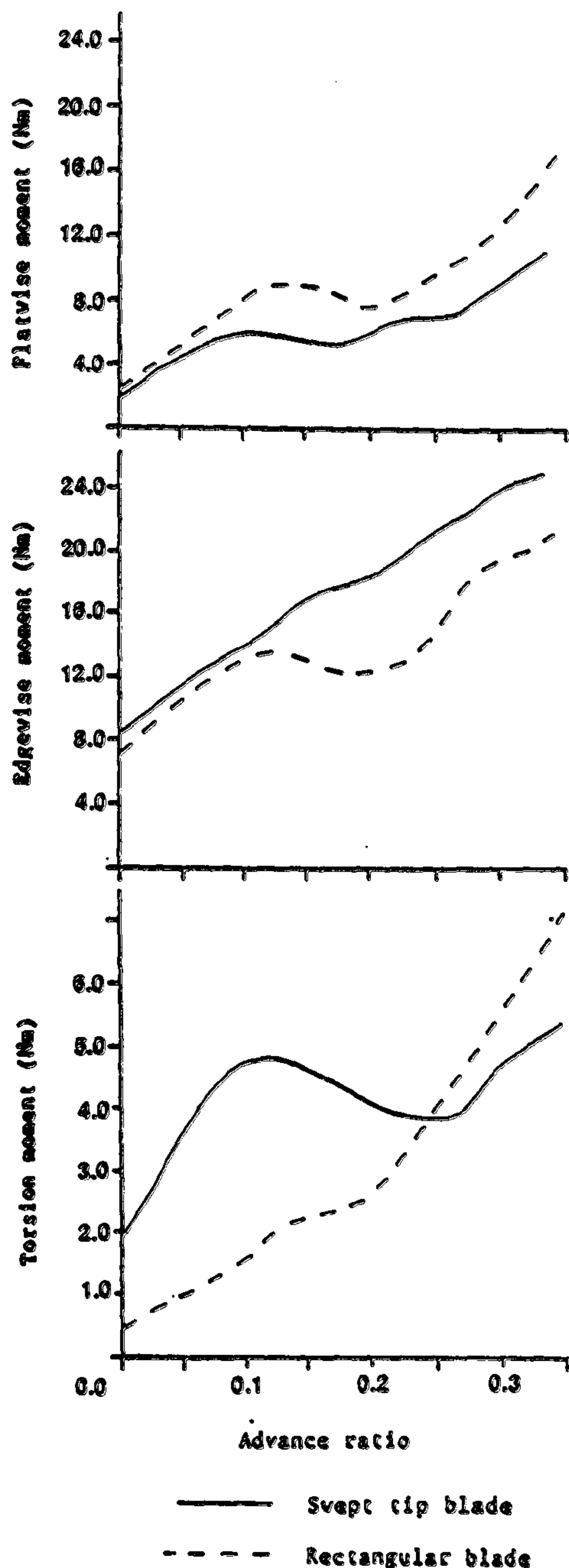


Fig 11. Measured blade root moments on the rectangular and swept-tip blades, thrust = 900N, $\mu = 0.34$

Fig 12 shows the variation of the peak-to-peak bending moments with advance ratio for both the swept-tip blade and for the rectangular blade. The flatwise moments are lower for the swept-tip blade and although the edgewise moments are slightly higher for the swept-tip blade, they are well within the design limits. The torsion moments are larger on the swept-tip blade at advance ratios below about 0.2, but lower at the higher forward speeds. On superimposing the azimuthal variation in the torsional moment for different advance ratios, Fig 13, there is a change in the shape of the waveform, particularly on the retreating side of the rotor disc, with advance ratio. The peak-to-peak moment is defined by the difference in moment at 280° azimuth with that at 340° azimuth at $\mu = 0.1$. However, at higher forward speeds the peak-to-peak moment is defined by the difference between the nose-down moment at 140° and the moment at 300° . There is also a change with advance ratio in the azimuthal position of the second nose-down excursion. Comparing these results with those from the tests on the rectangular blade, Fig 14, the very large trough on the advancing side of the rotor disc is the dominant feature. This trough is used to calculate the peak-to-peak moment for all advance ratios tested.



4. COMPARISONS BETWEEN EXPERIMENT AND THEORY

4.1 Correlation using the RAE/WHL coupled modes analysis

The measured and calculated bending moments at 37% radius are shown in Fig 13 for two advance ratios at 900 N thrust. The correlation between the two sets of results is reasonably good for the flatwise moments particularly for the lower forward speed. Some of the discrepancies are due to the underestimation of the 1R component at the lower advance ratio and 2R at the higher speed. One feature which has been predicted by the theory but is not present in the experimental results, is the high frequency oscillation on the advancing side of the rotor disc. This oscillation is due to the close proximity of the blade to the vortex shed from the tip of the preceding blade. There are some signs at other spanwise stations, not presented here, that the vortex is affecting the blade flatwise moment but the effect is less noticeable. This result shows the importance of being able to predict accurately the position of the tip vortex.

Corresponding plots for the edgewise bending moments are also shown in Fig 13. For increasing advance ratio the error in the amplitude of the 1R component increases although the phase difference between the experiment and theory decreases. At the higher forward speeds there is a pronounced 7R oscillation which arises from the second lag mode at $7.09R$ - Fig 9.

At an advance ratio of 0.1, the correlation between the measured and predicted torsion moments is fair but rapidly deteriorates at the higher forward speed. The main problem seems to be a change in the magnitude and a phase shift in the 3R component. There is also a significant higher harmonic content in the predicted results which arises from the second torsion mode.

Fig 12. Measured blade root moments, thrust = 900N

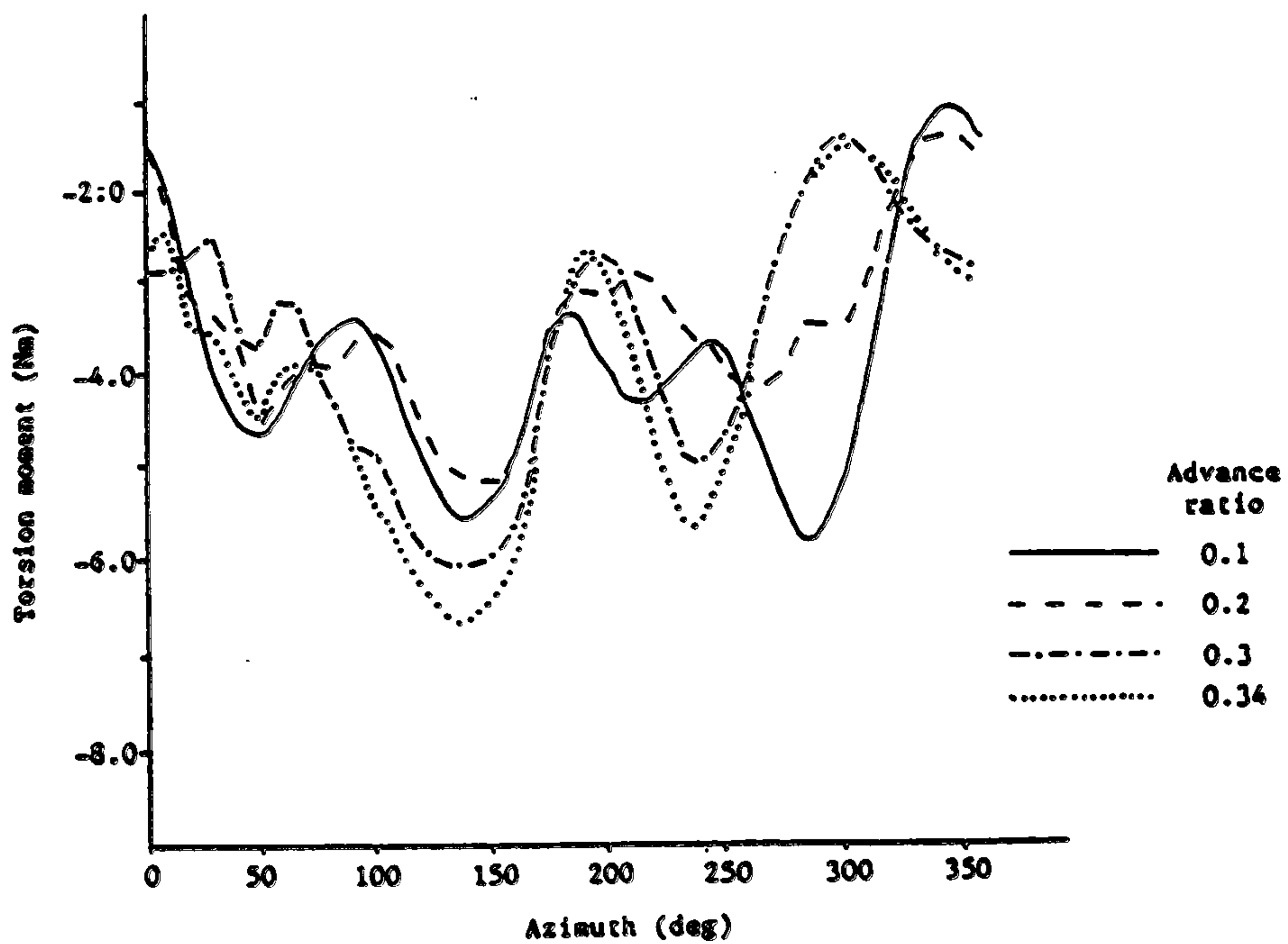


Fig 13. Measured torsion moment at the root of the swept-tip blade, thrust = 900N

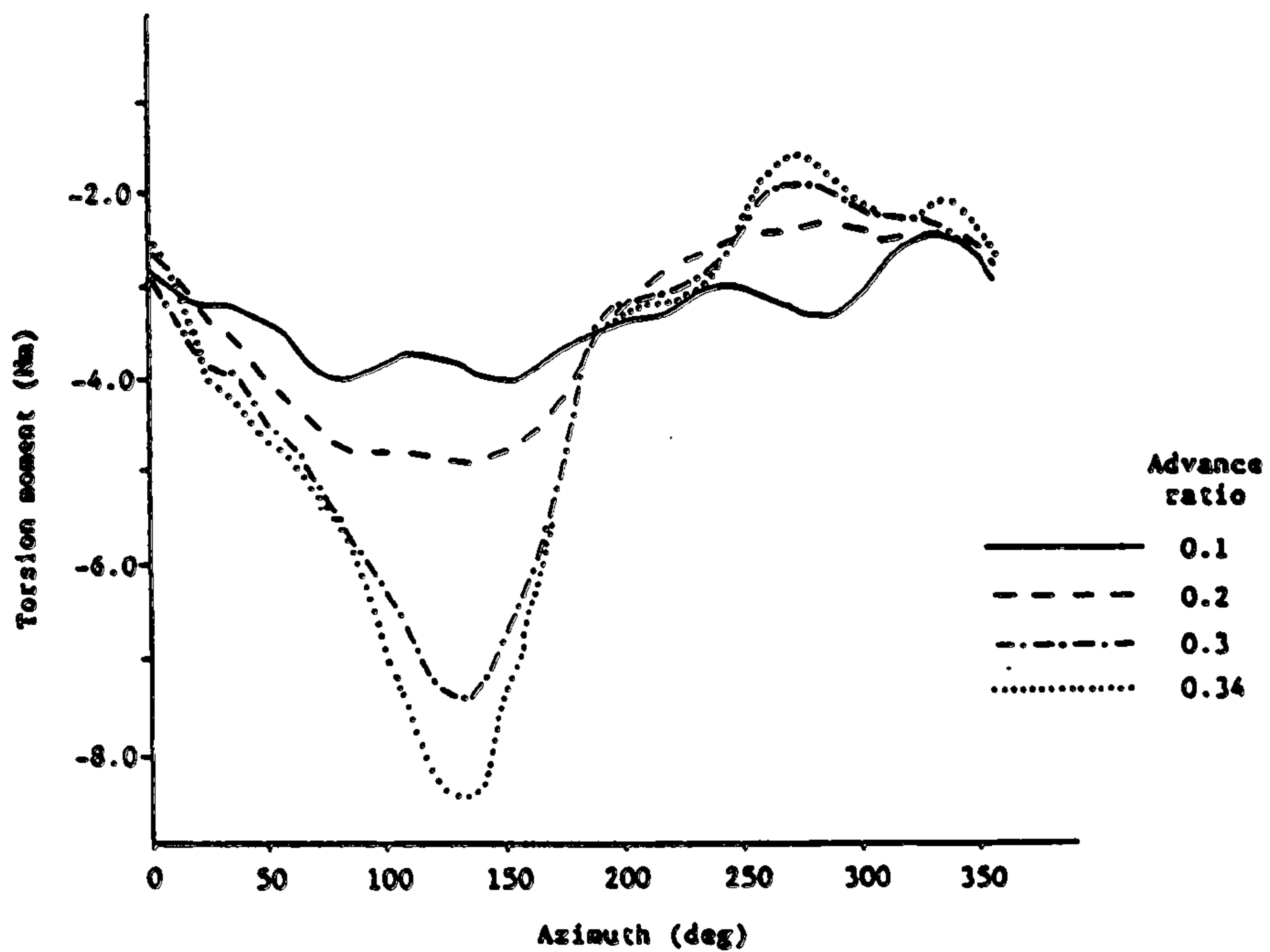


Fig 14. Measured torsion moment at the root of the rectangular blade, thrust = 900N

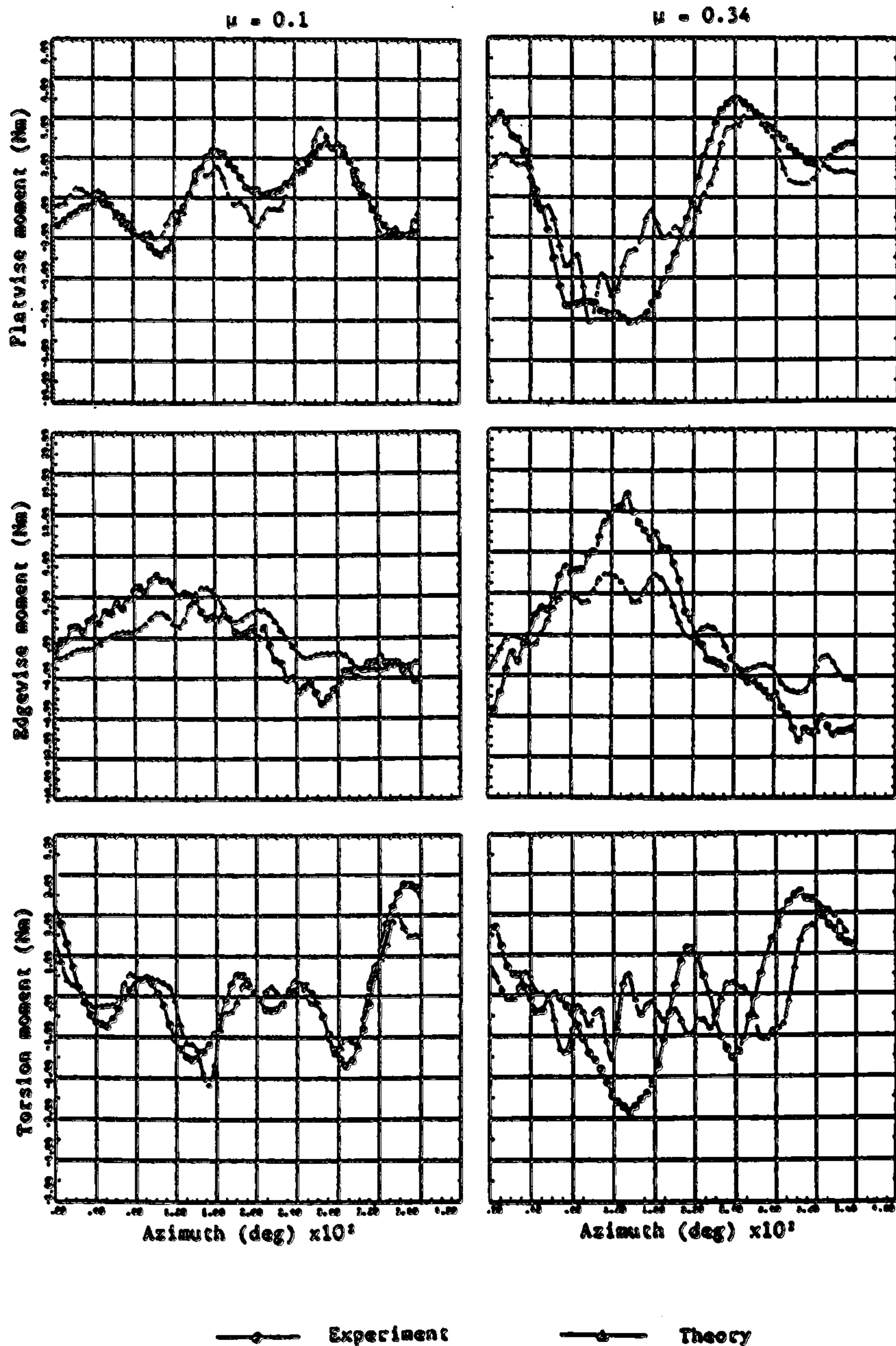


Fig 15. Measured and predicted blade root moments - 37% R, thrust = 900N

4.2 Correlation using an improved hub representation

The dual load path hub used in the Wind Tunnel tests, Figs 2 to 4, is a complex system to model accurately without the use of finite element techniques. A previous analysis of the results from a Wind Tunnel test on the baseline rectangular blade [12] has suggested that the representation of the secondary load paths by linear springs, Fig 16, is too simplistic. At present no account is taken of the fact that the inertia of the frame surrounding the coupling bearing is present only in flap and not in lag. Also, the rotational inertia about the flap axis for the coupling bearing and frame is not represented.

The coupled modes program used in the above analysis has been modified by WHL to analyse bearingless rotors for which a full representation of the two load paths can be incorporated into the rotor model. One of the load paths represents the flexible beam joining the blade to the hub whilst the second load path is used to model a torque tube connecting the control system to the blade root. Stiffnesses for the six degrees of freedom at the root of each flexure are also defined. A flexible element between the tips of the two load paths enables the flap and lag loads to be transferred to one of the flexures whilst the torsion loads are reacted by the other flexure. Although the dual load path hub is not a bearingless rotor, the two load paths can be used to produce a more accurate hub representation than used in the previous program, Fig 17. The rotational inertia however cannot be incorporated in the program.

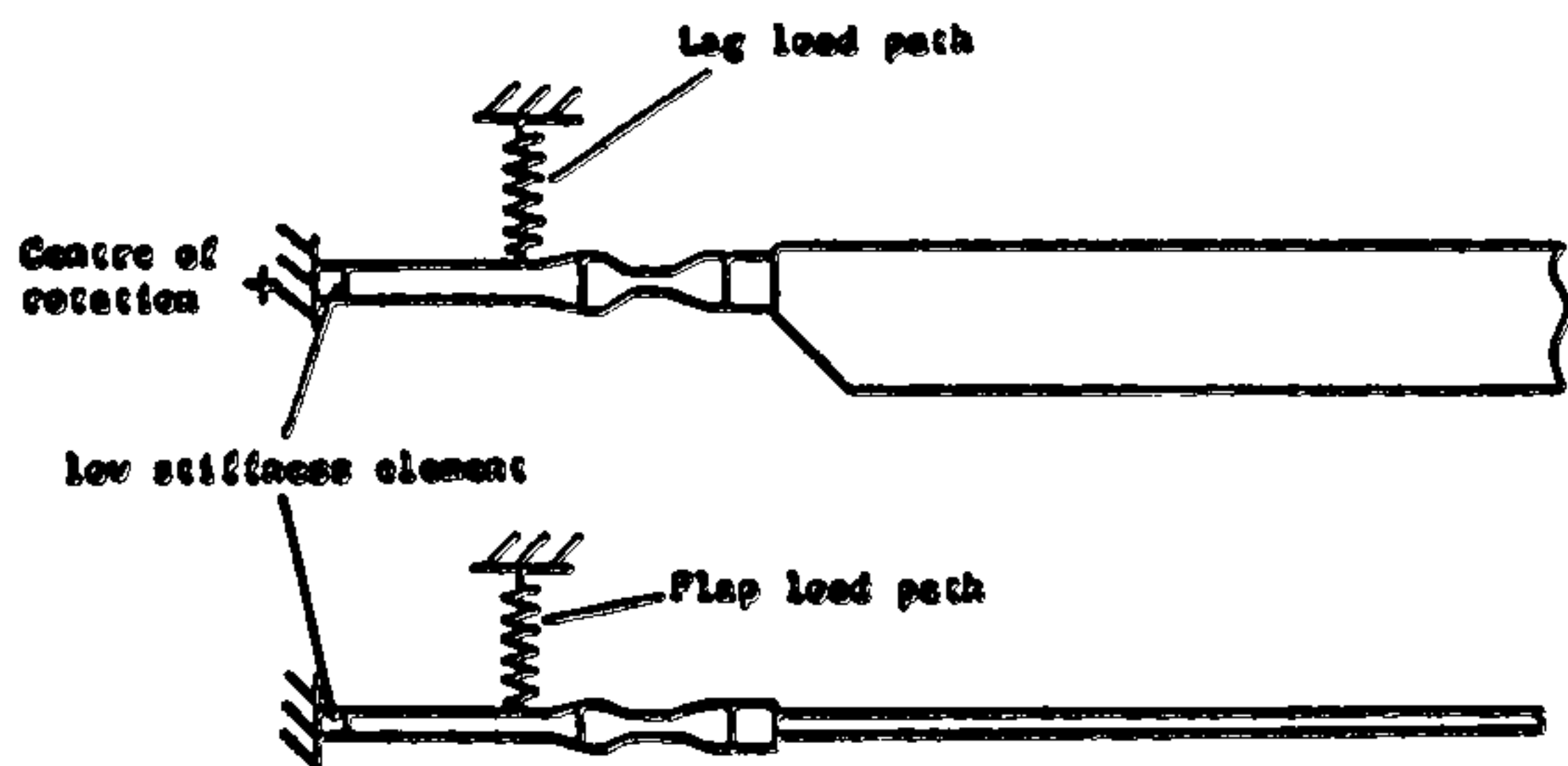
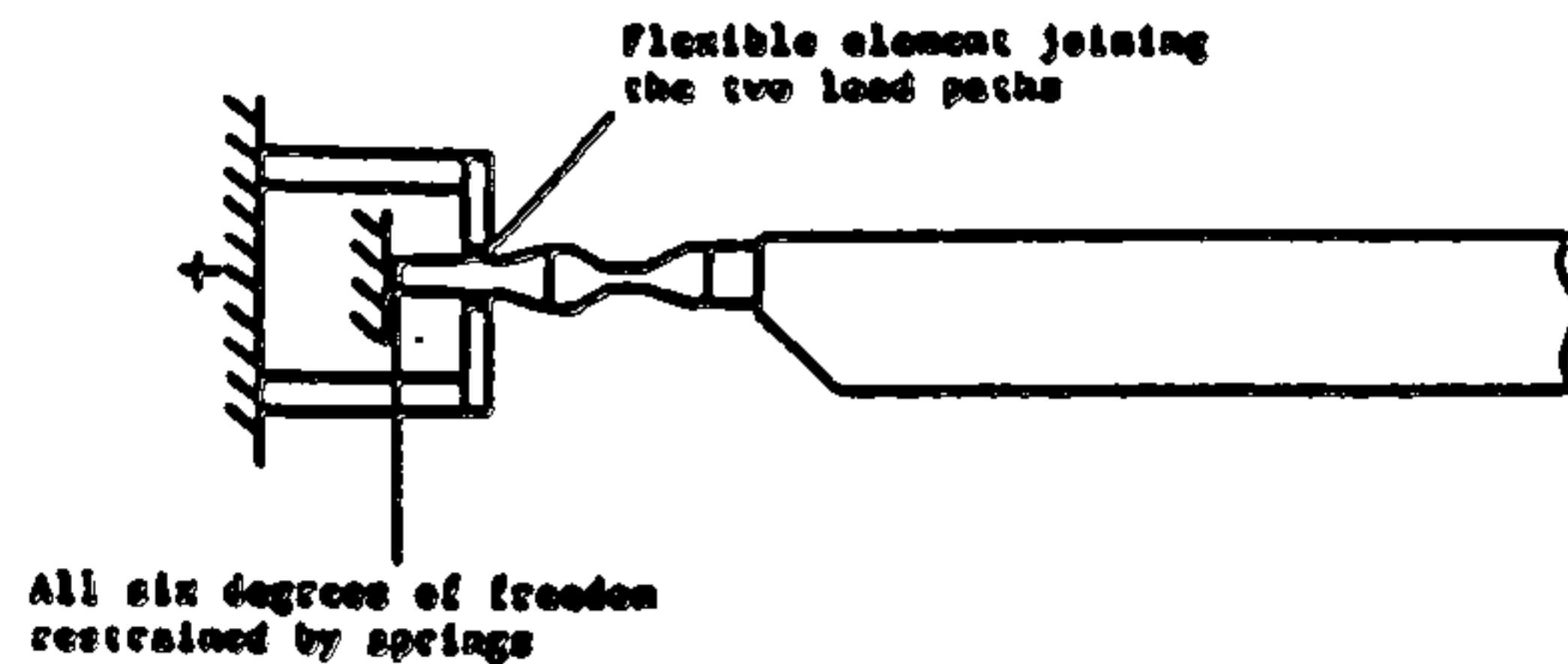


Fig 16. Hub model used in initial calculations

Fig 17. Hub model used in modified prediction program

The mode shapes calculated for the swept-tip blade using the modified program are shown in Fig 18. Comparing these with the modes produced by the previous program, Fig 9, the most significant change is the increase in the second lag frequency from 7.09R to 7.5R and a reduced coupling between lag and the other modes.

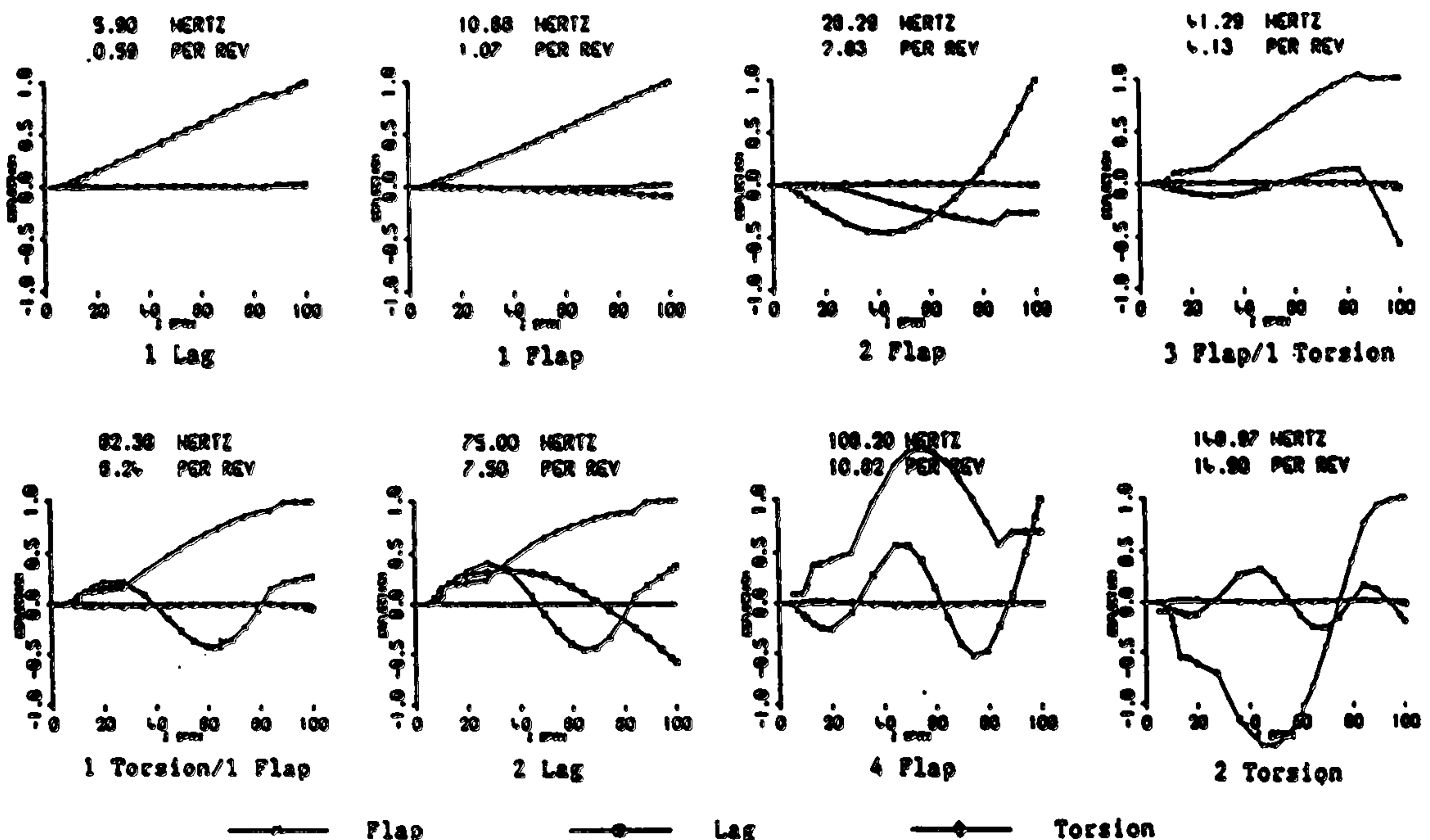


Fig 18. Mode shapes for the swept-tip blade

The measured and predicted flatwise bending moments are shown in Fig 19. There is generally very little change between these results and those using the previous program, Fig 15, although there is some improvement in the 1R amplitude at the high advance ratios. For the edgewise moments, Fig 20, the correlation is poor in terms of magnitude and phase. Finally, Fig 21 shows the torsion moments. On the advancing side of the rotor disc the magnitude of the nose-down peak is in good agreement especially at the higher advance ratios and also there is a significant improvement on the prediction made by the previous analysis, Fig 15. However, there is a need for improvements in the predicted phasing of the peaks on the retreating side of the disc.

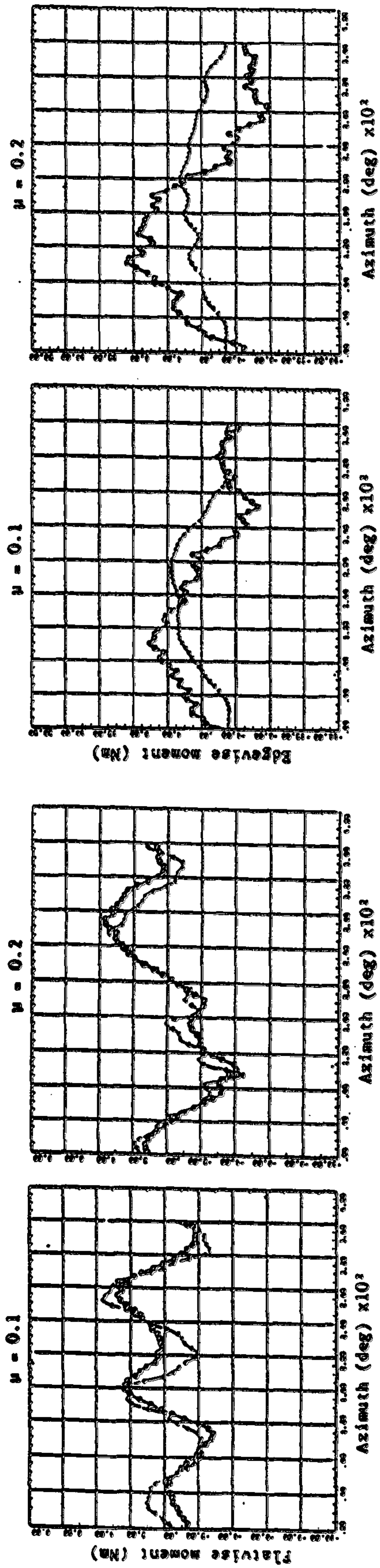


Fig 19. Measured and predicted flatwise moments for various advance ratios - 37% R, thrust = 900N

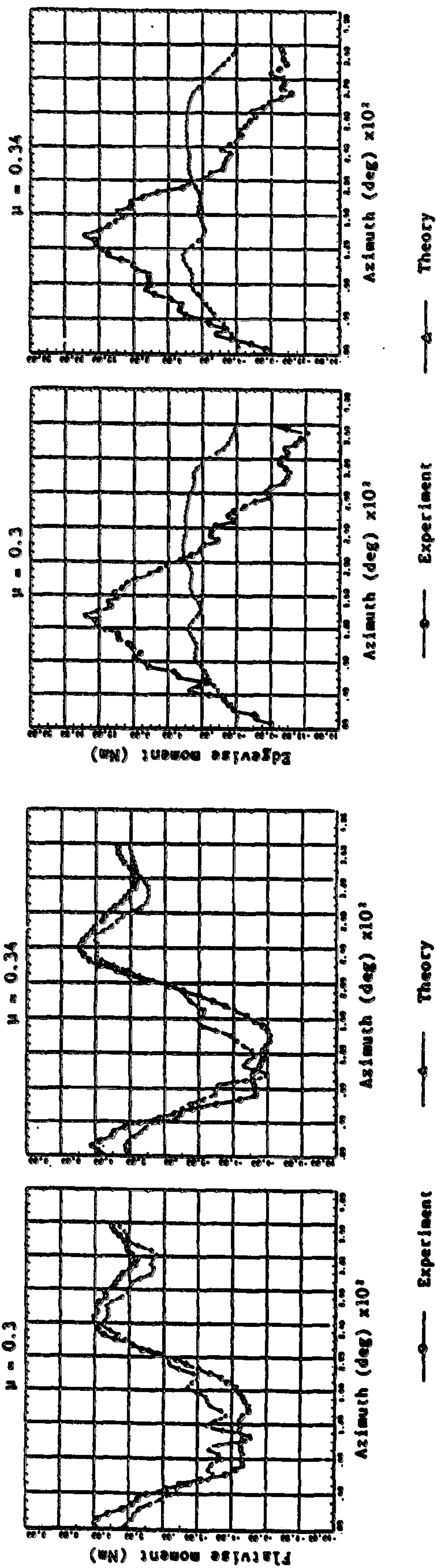


Fig 20. Measured and predicted edgewise moments for various advance ratios - 37% R, thrust = 900N

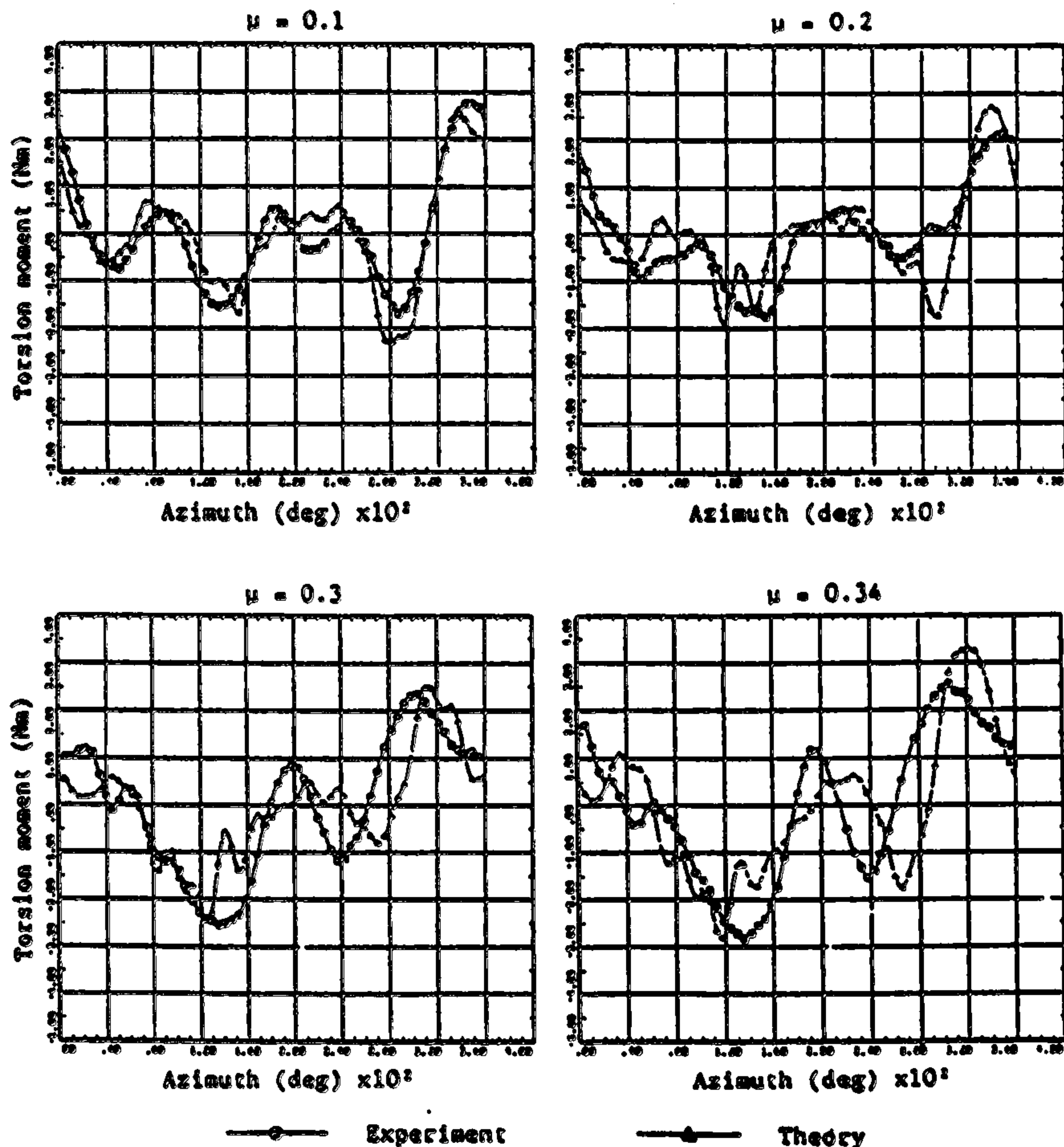


Fig 21. Measured and predicted torsion moments for various advance ratios - 37% R, thrust = 900N

Although there is room for improvement in the predicted variation of blade torsional moment, the gross effects of sweep back are nevertheless reasonably well represented. This gives confidence in the belief that the predicted effect of sweep back on blade twist (shown in Fig 22) is being achieved. Fig 22 shows the predicted variation in the twist of the swept-tip blade relative to the twist of the baseline-rectangular blade, the twist being defined as the difference between the blade tip and blade root geometric incidence. On the advancing side of the rotor disc, 90° azimuth, the twist decreases with advance ratio whilst on the retreating side, 270° azimuth, it increases at advance ratios above 1.8. This is one of the aims in the design of an aeroelastically adaptive rotor.

5. DISCUSSION AND FUTURE WORK

Comparisons between the measured flatwise, edgewise and torsion moments on the baseline rectangular blade and on the swept-tip blade show an azimuthal variation of blade twist has been achieved, whilst keeping the moments well within the prescribed limits. The swept tip has reduced the nose-down pitching moment on the advancing side of the rotor disc and hence the peak-to-peak moments are lower than those on the rectangular blade at high advance ratios. Further modifications to the blade design will probably reduce further the moments especially at the lower forward speeds.

The experimental results have also been compared with those predicted by the RAE/WNL coupled modes analysis. The correlations are fairly good for the flatwise and torsion moments at the low advance ratios. The calculated torsion moments are in poor agreement at the high advance ratios especially on the advancing side of the rotor disc. There is evidence of the second nose-down peak on the retreating side of the disc although it occurs at a larger azimuth angle.

A change in the modelling of the hub has shown that a significantly better prediction of the torsion moments can be achieved without affecting the accuracy of the flatwise predictions. The accurate prediction of these moments is particularly important whilst investigating aeroelastic rotors. Further work will however be necessary to resolve the discrepancies in the edgewise moments. Experimental work is planned to measure the non-rotating blade mode shapes and the rotating modal frequencies. The results would be very useful in identifying errors either in the prediction programs or in the way the rotor has been modelled.

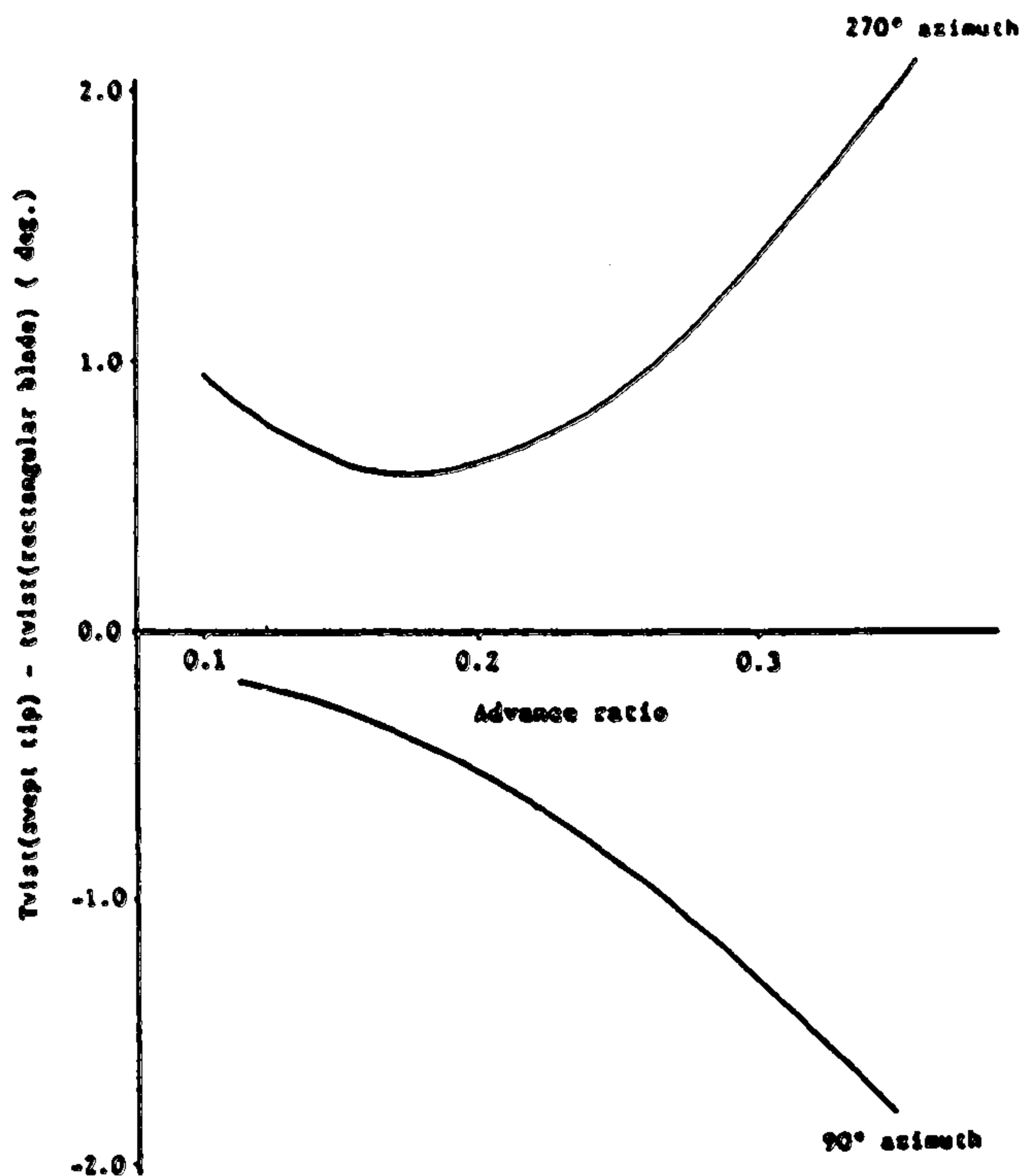


Fig 22. Predicted twist of the swept-tip blade relative to the twist of the baseline blade, 900N thrust

6. CONCLUSIONS

A swept-tip helicopter blade has been designed at model scale for testing in the RAE 24ft Wind Tunnel. An initial design suffered from a stability problem and was modified with the aid of a new modes/stability analysis and hover tests. The stability analysis was a useful tool in identifying the nature of the instability and for finding a solution.

The swept-tip blade has slightly different modal frequencies to the baseline straight blade due to an increased torsional stiffness and additional mass balancing. This has a small effect on the comparisons between the two blades and does not affect the main aim of the study which is to validate a rotor loads and performance analysis.

A comparison has been made between the blade bending moments predicted by the RAE/WHL coupled modes program with those obtained experimentally. The results show that the analysis is reasonably accurate for a large section of the flight envelope. The prediction methods can now be used to aid the design of blades incorporating further features of the aeroelastically tailored concept.

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APPENDIX C

ROTOR INSTRUMENTATION

C.1 Strain gauge installation

Full-size helicopter rotors blades are usually instrumented with strain gauges to measure the bending and torsional moments along the span during flight tests. Pressure transducers positioned spanwise and chordwise are used also for the calculation of the lift and pitching moment of the aerofoil. For the model swept tip blade experiments, discussed in the main section of the thesis, the main aim is to investigate the dynamic behaviour of the rotor and hence only strain gauges were installed.

The strain gauges were positioned on the blade surface with the wiring inside the blade in previous model rotor experiments. The advantage of that method was that the gauges could be replaced easily or repaired if damaged. The depth of the gauges was significant however compared with the aerofoil thickness and the protruding gauges could have an effect on the flow over the blade. An alternative method would have been to recess the gauges in the surface and this technique is commonly used for full-size rotors. For the baseline blade the strain gauges were mounted on the spar inside the blade as the blade fairing was very thin. The gauges were less prone to damage and did not interfere with the surface flow although they could not be repaired easily once installed.

The eight inboard strain gauge positions for the swept tip blades, Fig 19, were chosen to correspond to those for the baseline rectangular blade. Two additional gauges were placed on the swept portion and were positioned at 92% and 96% rotor radius. The moments measured were either parallel to or perpendicular to the local blade leading edge for each gauge station.

Fig C1 shows a typical strain gauge installation with flatwise, edgewise and torsional gauges mounted on the blade spar. A similar installation is on the lower surface of the of the spar (see also Fig D1). The two inboard gauges measure flatwise strains and the two outboard gauges measure edgewise strains. The gauges are of type WA-06-250BF-350 manufactured by Measurements Group Inc.. The rosette

mounted to the side and outboard of the above mentioned gauges, measures the torsional strains and is of type WA-06-250WR-350. The four gauges measuring each strain, for example the two flatwise gauges on the top surface and the two flatwise gauges on the lower surface, were connected to form a full strain gauge bridge as shown in Figs 77 and 78. This arrangement gave the maximum signal output whilst compensating for any thermal effects. The power supply was taken from two wires running along the leading edge of the spar and the six output wires from each strain gauge station were attached to the spar trailing edge. All of the wires were carried through to the blade root fitting.

A full set of strain gauges was used on only one of the three blades. The remaining blades had one strain gauge station located at 30% radius (station 1), but the cables were included so that the spanwise mass distribution was similar for all three blades.

Fig C4 shows the gauge installation points on the dual load path hub. As for the blades, only one arm of the hub was fully instrumented and only some gauges were duplicated on the remaining hub arms. The pitch link and the flap flexure gauges were used to measure a load rather than a bending or torsional moment.

C.2 Strain gauge calibration factors

The final data produced by the data acquisition system (see Appendix E) had to be converted from the system units into S.I. units in order to obtain meaningful results from the wind tunnel test data and to permit a comparison with theory. If a force 'P' is applied to the blade or to a hub component, the voltage across the corresponding strain gauges (R'), changes linearly with the magnitude of the force.

$$P = CR' \quad \dots(i)$$

where 'C' is known as the calibration factor.

For a general force $[P_1]$ which has flatwise, edgewise and torsional components P_{1f} , P_{1e} and P_{1t} respectively:

$$\begin{bmatrix} P_{1f} \\ P_{1e} \\ P_{1t} \end{bmatrix} = [C] \begin{bmatrix} R'_{ff} \\ R'_{fe} \\ R'_{ft} \end{bmatrix} \quad \dots(ii)$$

Two other forces P_2 and P_3 have to be applied to calculate the elements of the 'C' matrix.

$$\begin{bmatrix} P_{1f} & P_{2f} & P_{3f} \\ P_{1e} & P_{2e} & P_{3e} \\ P_{1t} & P_{2t} & P_{3t} \end{bmatrix} = [C] \begin{bmatrix} R'_{ff} & R'_{ef} & R'_{tf} \\ R'_{fe} & R'_{ee} & R'_{te} \\ R'_{ft} & R'_{et} & R'_{tt} \end{bmatrix} \quad \dots(iii)$$

If the loads are applied in the flatwise, edgewise and torsion directions i.e. in plane to the direction of maximum response for the corresponding strain gauge, $[P]$ reduces to a diagonal matrix.

$$\begin{bmatrix} P_{ff} & 0 & 0 \\ 0 & P_{ee} & 0 \\ 0 & 0 & P_{tt} \end{bmatrix} = [C] \begin{bmatrix} R'_{ff} & R'_{ef} & R'_{tf} \\ R'_{fe} & R'_{ee} & R'_{te} \\ R'_{ft} & R'_{et} & R'_{tt} \end{bmatrix} \quad \dots(iv)$$

(The off-diagonal terms of the " R' " matrix should be small compared with the diagonal terms). For unit applied loads or moments:

$$[C] = [R']^{-1} \quad \dots(v)$$

The calibration matrix can now be used to obtain the blade moments and hub loads in SI units from the data collected during the wind tunnel test. There is however one assumption and that is that the

supply voltage during the bench calibration is the same as the supply voltage during the experiment. (As explained in Section C.3 the gauges were calibrated on a separate rig to the wind tunnel test rig and therefore, a drift in the power supply is not the only source of error). A single compensating factor could not be used for all of the gauges as the supply voltages during calibration could vary also between gauges. Similarly, each gauge station could have a different power supply during the wind tunnel experiment.

The ratio between the sum of the voltages across each arm of the strain gauge bridge and the applied load during a bench calibration, was recorded by the Data Acquisition System described in Section C3. In addition, the strain gauges were replaced by known resistors and the voltages across each one, were summed. This is used to compensate for any voltage changes between the bench calibration and the calibration prior to a wind tunnel test. Finally, the distance between the strain gauge position and the point at which the load was applied, was recorded so as to obtain the required unit applied moment.

Taking one of the terms in the " R' " matrix as an example:

$$R'_{fe} = G' / LV_a \quad \dots(vi)$$

where " R' _{fe} " corresponds to the response on an edgewise gauge due to an applied flatwise load, 'L' is the moment arm of the load, ' V_a ' is the summed voltage across known resistors and " G' " is ratio of strain gauge output to the applied load.

The same resistors as used in the bench calibration were put round the bridge arms before each set of runs in the wind tunnel. This gave the total voltage [V_t]. The supply voltage to the gauges 'V₁' was also recorded as all of the components of [V_t] are proportional to

V_1 . For each test condition during a tunnel session, the gauge supply voltage V_2 was re-recorded and used to compensate for any further fluctuations.

Combining the calibration matrix with the final gauge voltages $[V_G]$ for an unknown applied force $[P_A]$:

$$\begin{bmatrix} P_A \end{bmatrix} = \begin{bmatrix} C \end{bmatrix} \frac{V_1}{V_2} \begin{bmatrix} V_t \end{bmatrix}^{-1} \begin{bmatrix} V_G \end{bmatrix} \quad \dots(vii)$$

C.3 Strain gauge calibration

A rig was designed at the RAE, Fig C5, to ease the calibration of the strain gauges. The rig is made up of three main parts. The blade root fitting is clamped onto a rigid column via a rotating plate. The plate allows the blade, which is twisted, to be aligned so that the coupling between the applied flap and lag loads is minimised. A clamp is positioned along the blade tip onto which weights are hung depending on whether a flatwise, edgewise or torsional moment is required. A frame with pulleys is used to aid the application of the weights. The signals from the strain gauges are then passed through amplifiers to the data acquisition system. The variables recorded by the system were: the voltage across each of the flatwise, edgewise and torsion strain gauge bridges and the power supply voltage for each applied load, and the sum of the voltages around known resistors. The data was used to form the components of $[R']$ which was inverted to form the calibration matrix $[C]$ in equation (viii) of the previous section.

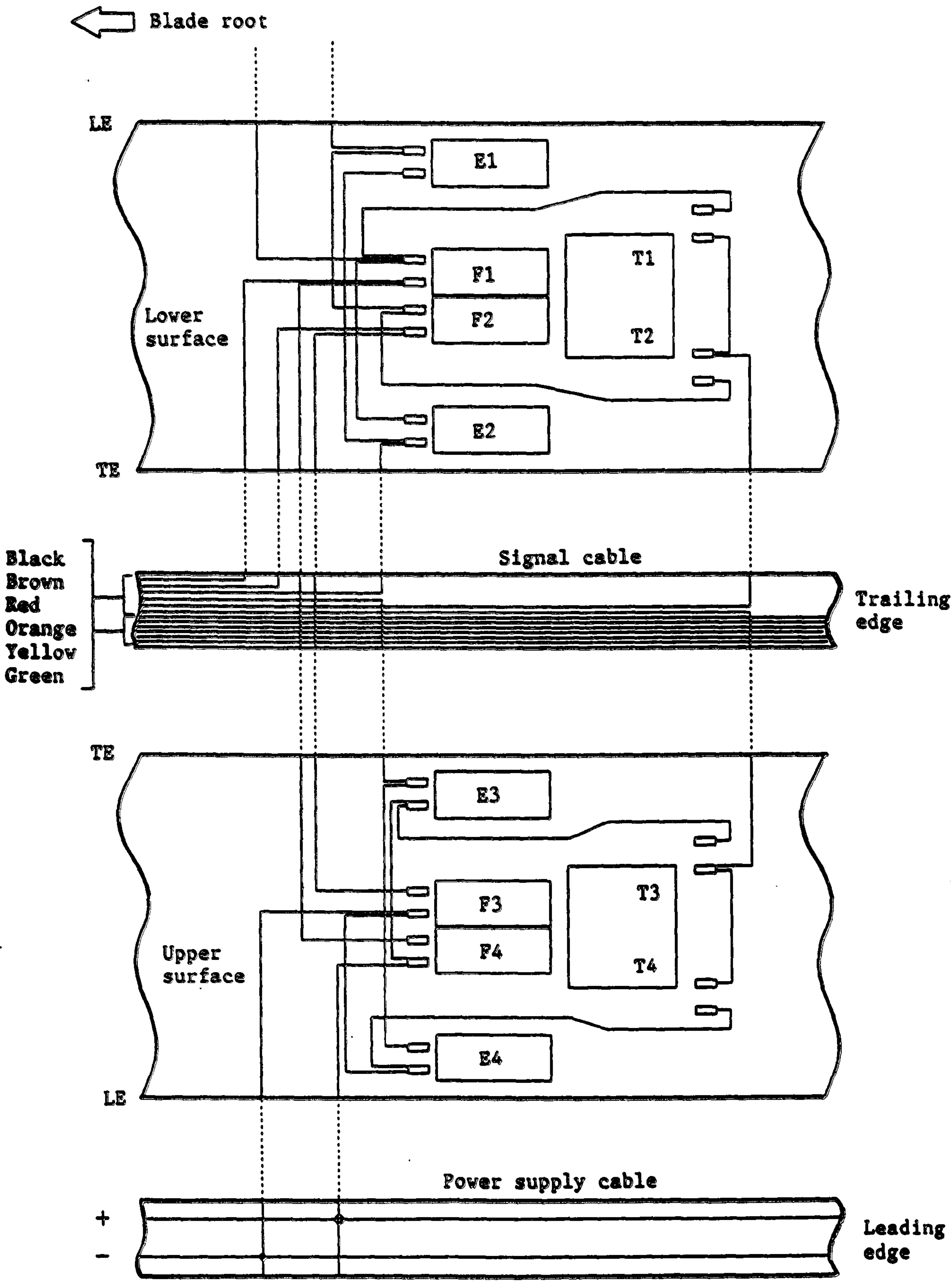
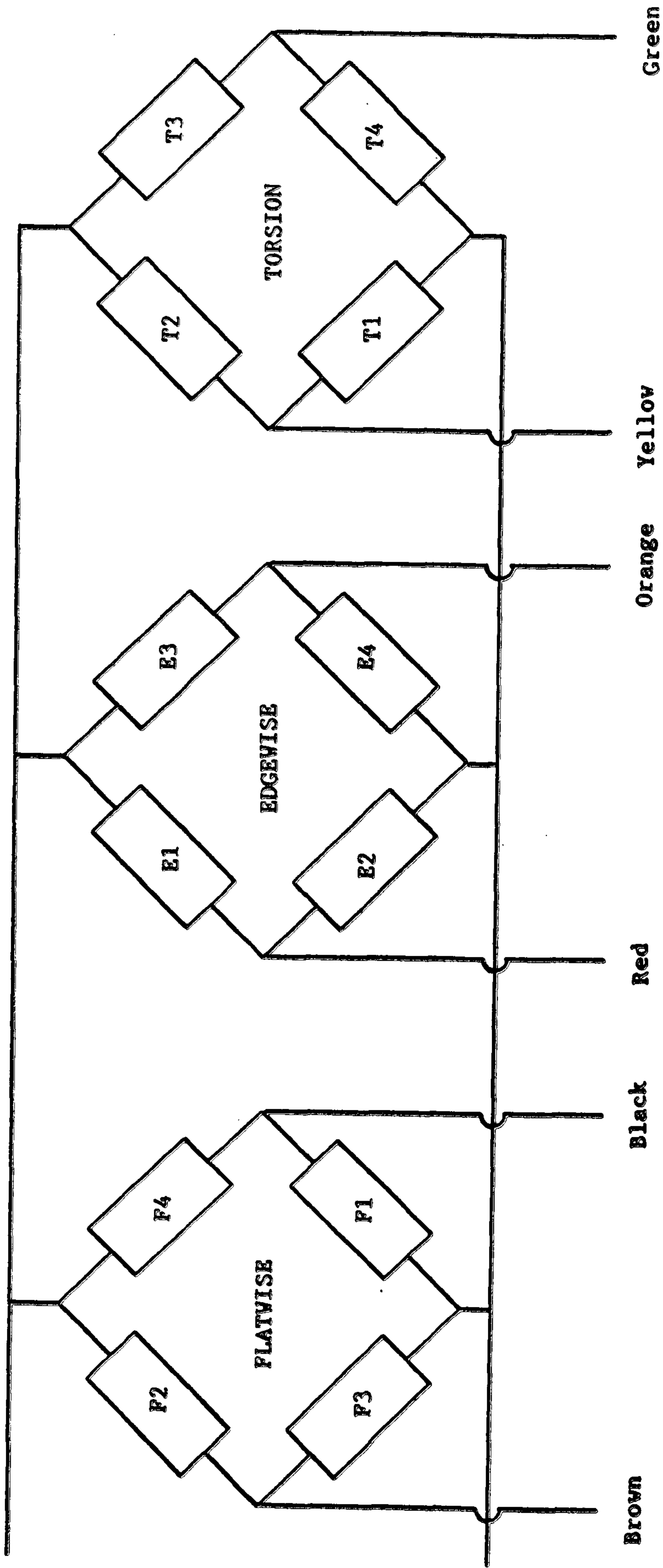


Fig C2 Strain gauge wiring



Flatwise and edgewise gauges :- Measurements Group Inc WA-06-250BP-350

Torsion gauges :- Measurements Group Inc WA-06-250VR-350

Fig C3 Strain gauge bridges

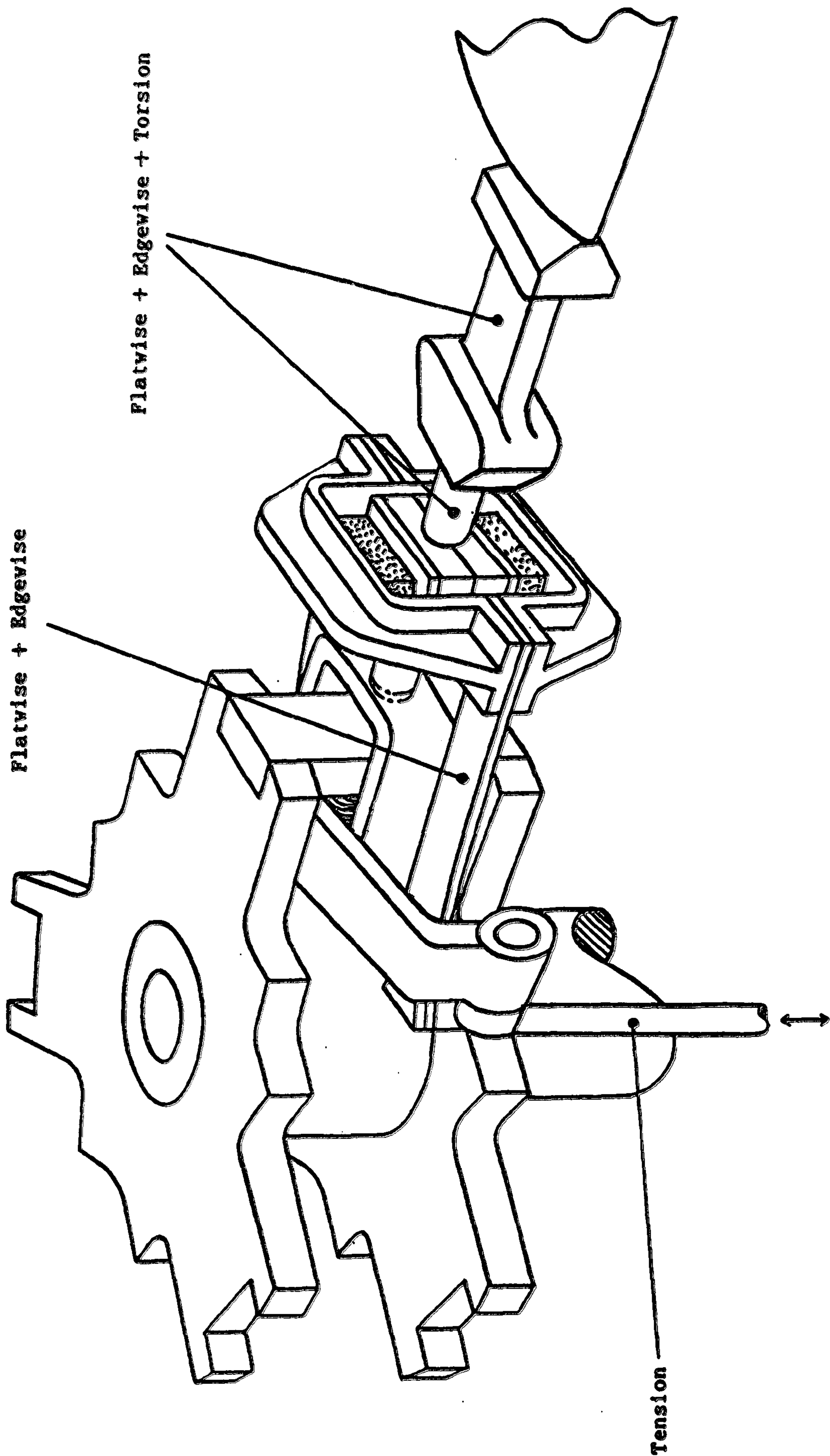


Fig C4 Dual load path rotor system showing strain gauge installation

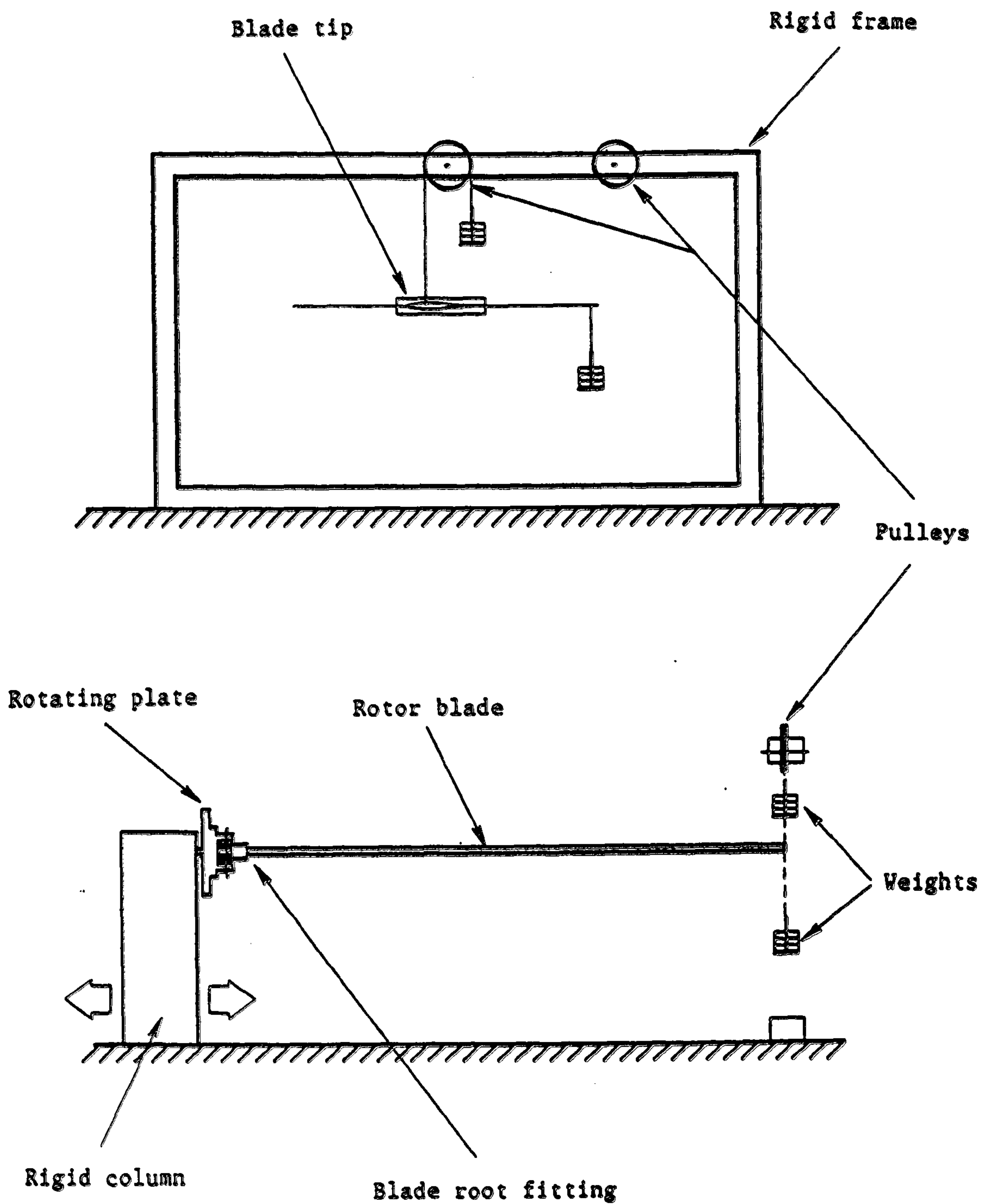


Fig C5 Calibration rig .

APPENDIX D

SWEPT TIP BLADE MK2 BLADE SPECIFICATION

The construction and manufacturing technique of the swept tip blade was very similar to that of the baseline rectangular blade[44]. Some of the differences and more important details are discussed below.

D.1 Blade spar

The blade spar was made up of a Rohacell foam core sandwiched between two sheets of unidirectional carbon fibre. A cross-section of the blade spar is shown in Fig D1. One of the problems encountered in the design of the rectangular blades was that the electrical insulation between the carbon fibre and the strain gauges broke down following a reheating cycle whilst rebuilding the blade. A layer of Kevlar was introduced therefore and also a thin layer of glass cloth added to improve the surface finish prior to the installation of the strain gauges.

It was decided that the swept portion of the blade (Fig D2) would have strain gauges installed although the rectangular blade did not have any corresponding gauges. It was necessary therefore for the spar to be continued into the tip region. A number of solutions were tested for the manufacture of a spar with a 20° kink. The kink had to withstand the edgewise moment produced by the centrifugal force. The solution chosen for the design was to realign the fibres at 20° to the main section of spar. The strands of fibre from the pre-preg carbon were separated into narrow strips and were realigned along the swept part of the blade. This avoided the need for a joint in the structure which would have been weaker than using continuous fibres. The layers of composite material with the foam core were placed in a mould of rectangular cross-section. The mould was designed to have a low torsional stiffness so that it could be twisted to the required angle. Spacers were added between the mould and the heated press, the thickness of the spacers being dependent on the required pre-twist. An aluminium block was added also into the spar tip so that screw-in weights could be used to balance the blade in the spanwise direction.

The strain gauges were added using a hot cure adhesive following the curing of the resin. The strain gauge installation is described in Appendix C. Finally, the gauges were checked for correct operation.

Fig D2 shows the dimensions of the root and tip sections of the spar. The tip was cut at an angle of 87.8° so that the chordline made a tangent to the circle described by the blade as it rotated. The angle took into account the 2 deg sweep at the swept link and the 20° sweep of the tip.

D.2 Foam core

The spar was bonded into a 51 grade Rohacell foam core which had been shaped to the correct aerofoil section. A 312UL film adhesive cured at 393K was used. The main and swept portions of the core were joined together with 312 film adhesive.

D.3 Blade fairing

The inboard part of the blade, Fig D3, was covered with unidirectional glass fibre pre-preg using the 913 resin system. The resin had a lower curing temperature than the 914 system for the spar, hence the second heating cycle did not affect the already cured spar. The large number of layers of glass fibre around the leading edge were required so as to position the shear centre near to the quarter chord. A 10mm wide strip of carbon fibre was added above and below the blade quarter chord to meet the higher torsional stiffness requirements of the Mk2 blade. Each strip was made up of two layers of fibre angled at $\pm 45^\circ$, thus increasing the torsional stiffness and only slightly changing the flatwise and edgewise stiffnesses. A narrow strip of glass cloth was added around the trailing edge as reinforcement.

The tip section, Fig D4, was made up of $\pm 30^\circ$ Kevlar which increased the torsional stiffness to nearly three times that of the inboard section. The layers of Kevlar were joined around the leading

edge with unidirectional Kevlar to allow the fibre angle to be changed from $+30^\circ$ to -30° and to keep the layup symmetrical. A thin layer of glass cloth was used again to improve the surface finish. A 16mm wide strip of unidirectional carbon fibre was positioned over the quarter chord to increase the flap stiffness to that of the inboard section.

The layup from the main section of blade overlapped the tip Kevlar fairing by 20 mm and also continued onto the root fitting. A layer of 312 film adhesive was put between the layup and the root fitting to give a better bond, and a layer of 913 glass pre-preg with its fibres running chordwise, was positioned around the blade root to prevent splits starting as a result of a stress concentration at the blade root fitting.

D.4 Mass balancing

Fig D5 shows the position of the 80 gramme mass added to the blade in order to enhance the aeroelastic stability of the blade. The usual leading-edge weight extended only to the blade/tip junction.

The mass and the spanwise centres of gravity were made consistent between each blade by the addition of weights into the aluminium tip block and onto the blade root fitting. Finally a balsa tip fairing was added. Alignment marks and an identification number were added onto the tip fairing to assist blade tracking.

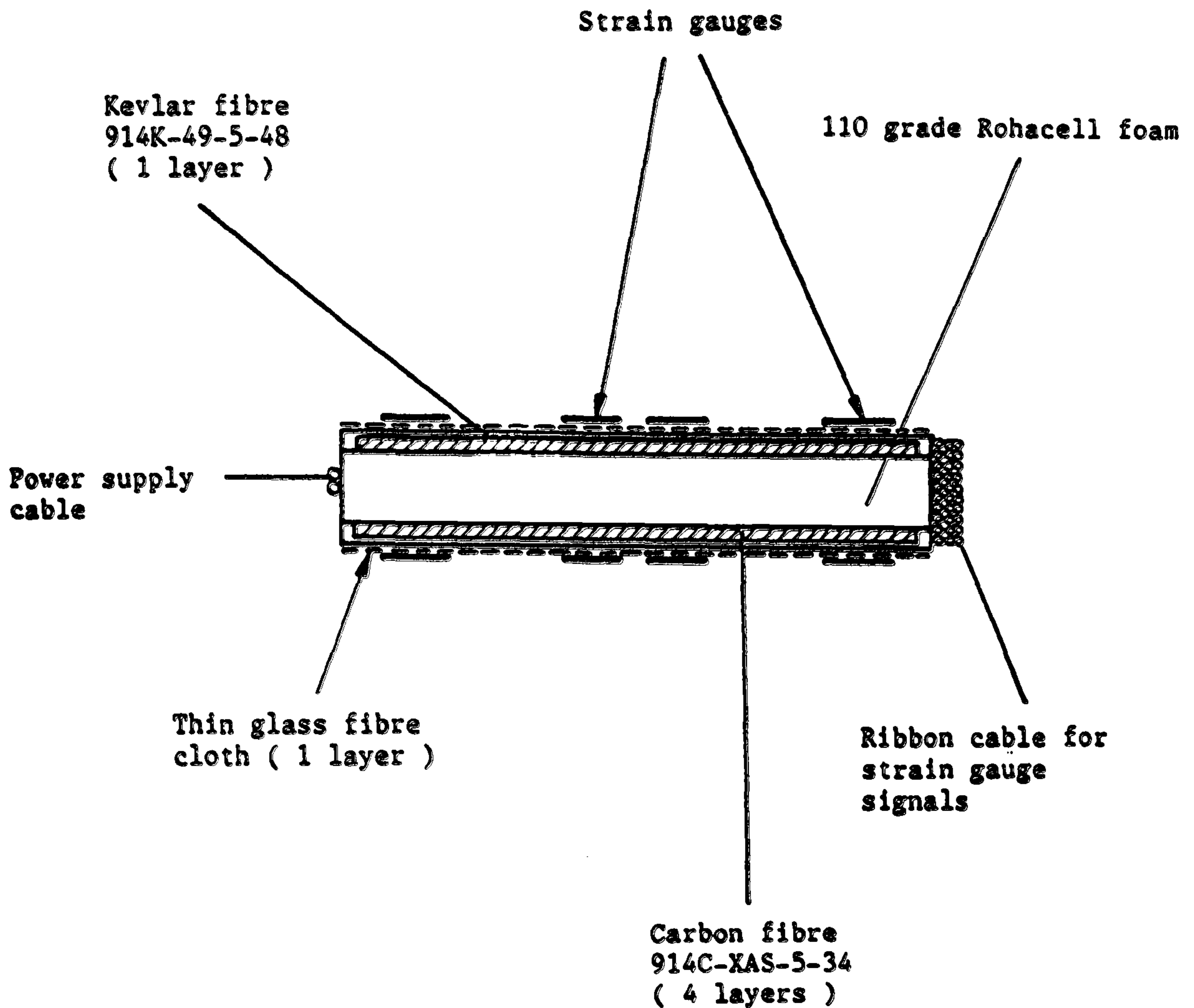


Fig D1 Blade spar construction

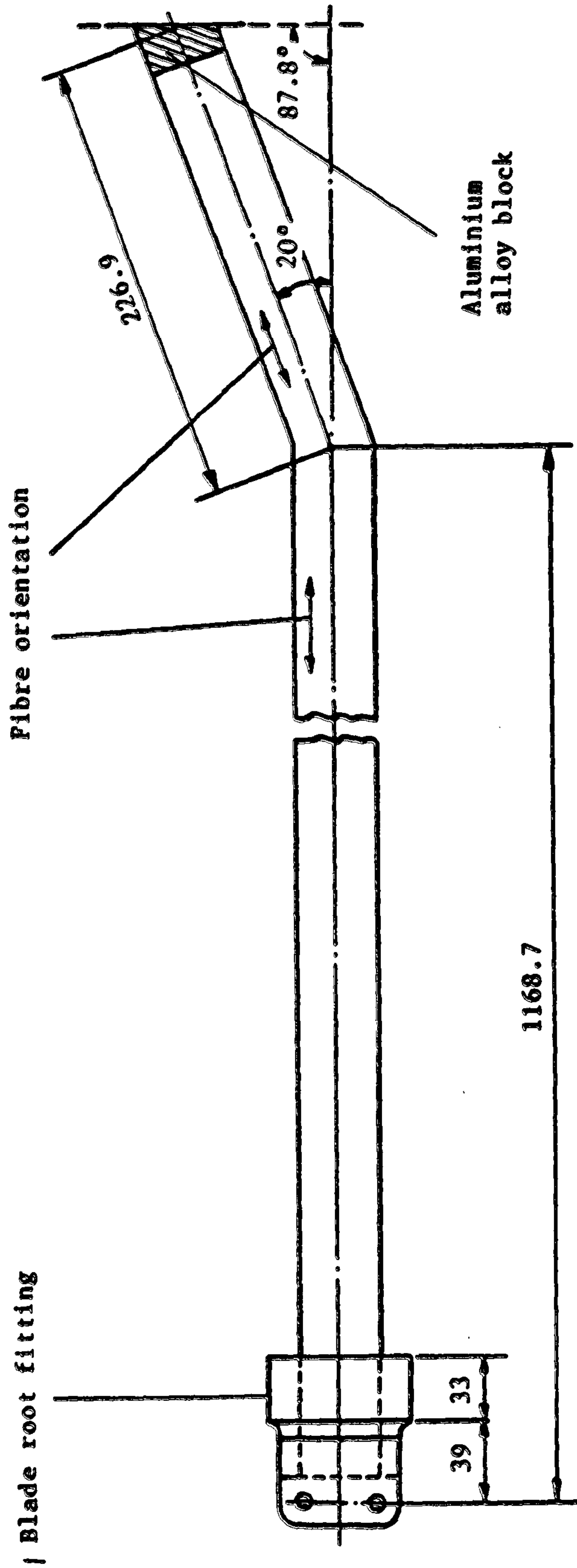


Fig D2 Spar dimensions

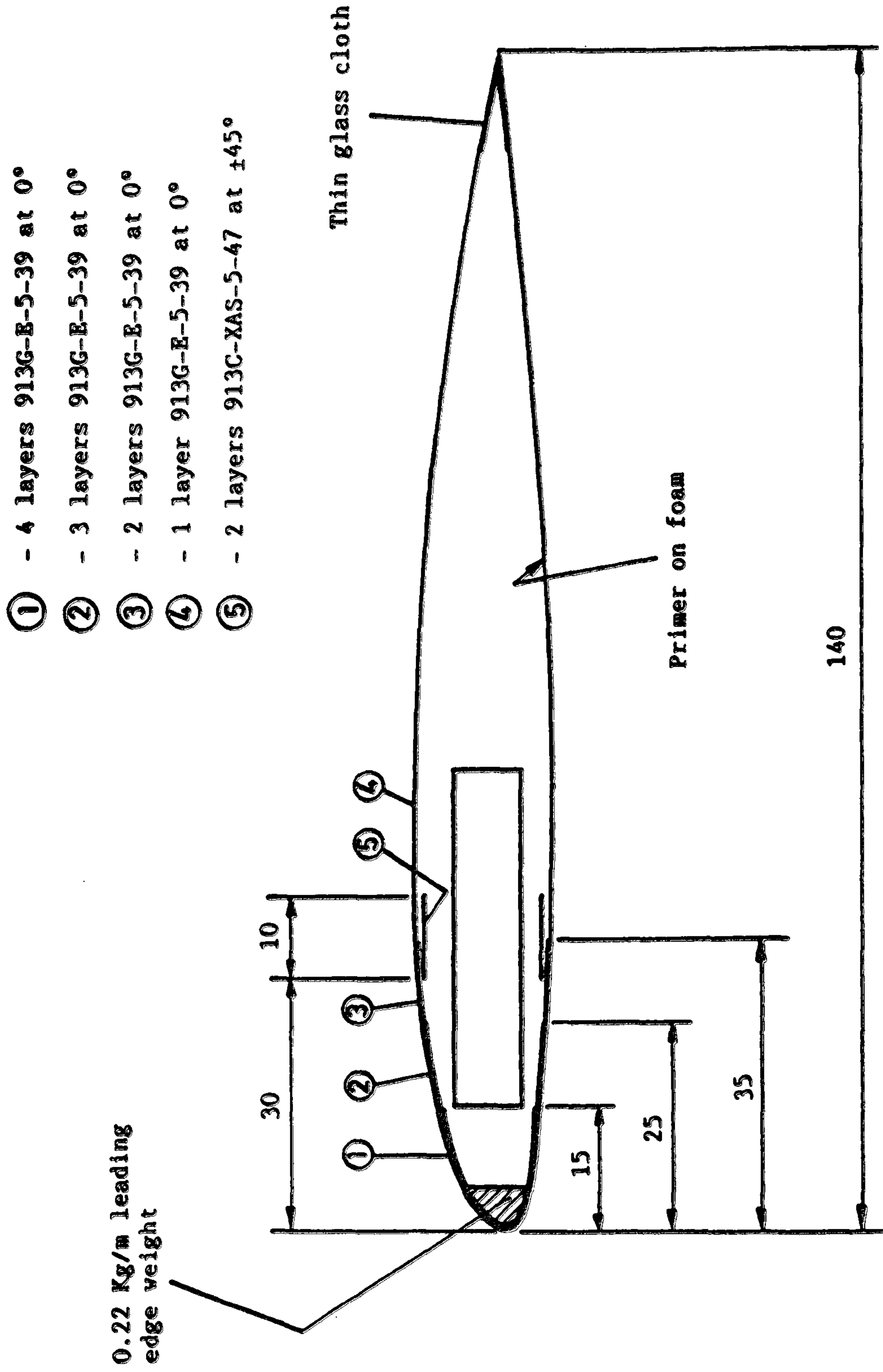


Fig D3 Construction of the main section of blade

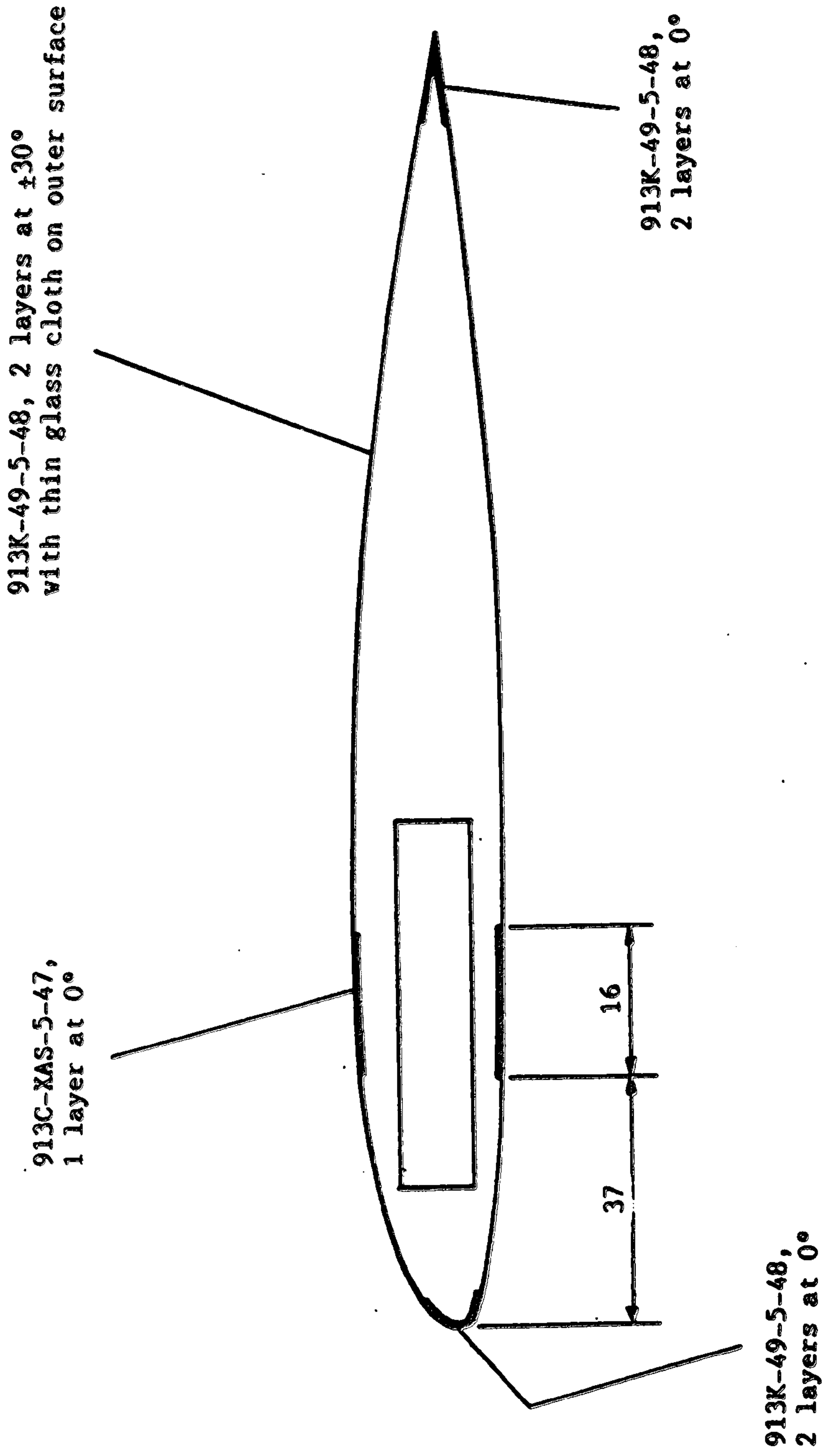


Fig D4 Construction of the blade tip section

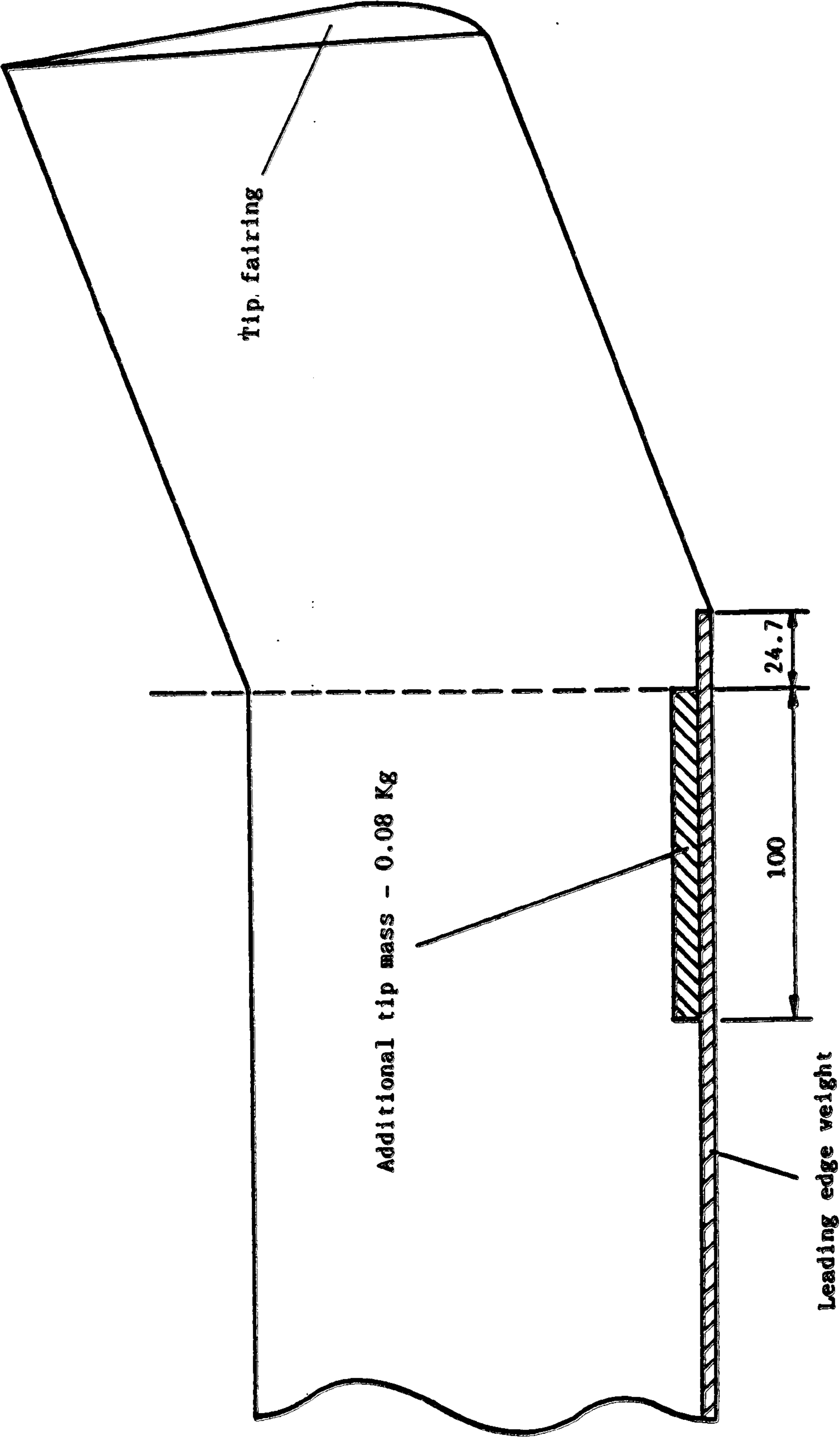


Fig D5 Blade mass balancing

APPENDIX E

WIND TUNNEL DATA PROCESSING

The most important aspect of any experiment is the processing of data. It is necessary to ensure that a high accuracy is maintained so as to avoid large experimental errors. One feature of most helicopter rotor tests is the large number of test cases and the vast amount of data that is recorded for each case. This section therefore also includes a description of the 'book keeping' and 'data checking' features used during the processing of the test data.

E.1 Data stored

The signals from the strain gauges pass through the first stage amplifiers, through the slip rings and then onto the computer via the A/D converters and the Data Acquisition System (DAS) - see Appendix F. Other signals such as the the position of the swash plate actuators, the torque and rpm values and various power supply levels also pass down to the computer. At the beginning of each tunnel test each signal from the rotor tower was associated with a particular channel of the DAS. A data file containing the allocation of the various signals to the DAS channels was set up on the HP 1000 computer. The data file contained also the maximum and minimum values of the expected signals and whether the channel was active or not in use. During a test a warning was given if the limits for a particular data channel were exceeded. If the particular signal was vital for the experiment then the test could be stopped and repairs could be undertaken. Otherwise the channel was switched to ' not in use ' and the limits would no longer apply. The test could then continue without any further delay.

The time and date of the test and the test number were recorded first by the computer and stored in a file known as the 'on-condition' file. The relationship between the DAS channel number and the type of signal associated with it, was copied from the file mentioned above to the 'on-condition' file. The data that recorded by the DAS was to a twelve bit accuracy and hence the data had to be in the range of -2048 to +2047 Data Acquisition System Units (DASU). There is a direct relationship between the voltage across a strain gauge bridge and the value in DASU. To maximise the resolution of the data, one of four

values of DASU, either positive or negative, could be added to each channel to minimise the dc. For this purpose a sample of data was taken to establish the particular value of offset. The stored signal contained mainly the ac component and if the signal was small compared with the -2048 to +2047 range, the gain on the first stage amplifiers could be changed to a factor of two. Both the gains and offsets were stored along with whether the channel was in use.

The signals from the strain gauges were passed through two sets of amplifiers as mentioned in Appendix F. The signals incurred therefore a phase shift which depended on their frequency. To compensate for the phase shift the relationship between a known signal, in that case a frequency sweep, and the output after passing through the same amplifiers was required. The next set of data recorded was therefore the response to the known signal.

The strain gauge signals were recorded next and it was possible to average the data over up to sixteen revolutions. The DAS has only sufficient memory for data from one revolution of the rotor i.e. 64 channels recorded at 256 azimuth points. Data from subsequent rotor revolutions could not therefore be recorded as time between each revolution was required for the transfer of data from the DAS to the computer local memory. Finally, the rotor rpm was recorded.

E.2 Data processing

Section F.4 describes the wind tunnel test procedure and at the end of a test the data files that were stored are:

- a). A table of calibration matrices for the various strain gauges.
(see Appendix C).
- b). A table of voltages [V_t] for each strain gauge bridge obtained at the beginning of each day or following a configuration change.
- c). The datum file which contains strain gauge readings with the rotor at 20 rpm.

- d). The files containing the gauge readings taken during a particular flight case (the 'on-condition' files described above).

The first stage in the processing of the data was to use the stored gains and offsets to restore the ac and dc magnitudes of each data channel. The next stage was to correct for the amplifier phase shift. The relationship between the input frequency sweep to the amplifiers and the output response signal stored in the on-condition file was calculated using a Fast Fourier Transform (FFT). The reverse relationship was applied to the recorded strain gauge signals thus giving the true phasing of the signal. The phase shift correction and the application of the gains and offsets was repeated for the datum signals.

The final stage in the evaluation of equation (vii), Appendix C, was to extract the voltages V_1 , V_2 and V_t from the data files and multiply these by the calibration factors. The datum results were then subtracted from the flight case results.

The output file contains the loads or bending moments for up to forty transducers and with 256 readings for one rotor revolution. An additional post-processing stage rearranges the data into a form that can be used directly by the DATAMAP analysis/plotting package. With the theoretical results and previous experimental results also included in the DATAMAP data base, comparisons between the different results can easily be made.

E.3 Data Acquisition System Post-Processing Programme

The various stages of data processing mentioned in section E.2 were originally set up on an HP1000 computer along with the DAS. The processing however only included the FFT, the application of the gains and offsets and the subtraction of the datum values. The matrices from the component calibrations were assembled and inverted manually and then hard-wired into the programme for the remaining processing stages. This method had the potential of introducing errors into the

wind tunnel test data and the process was also very slow. For these reasons the processing of the raw data was incorporated into a single package which was both easy to use and minimised the number of potential sources of error. A detailed description of the DAS Post-Processing Programme (DASPPP) can be found in Ref E1 and only the most important features are mentioned below.

The data from the bench calibrations was transferred from the HP1000 computer to a VAX 11/780 via magnetic tape. The calibrations were assembled and placed into a data base using the menu driven programme DASPPP. For each wind tunnel test configuration the various calibrations for the blades and hub were associated with a DAS channel number and the results were stored in a file. The file could then be used for the processing of the wind tunnel data.

The test data was transferred also onto the faster VAX 11/780 and under the control of DASPPP various files can be selected and processed either interactively or remotely. The main output file containing the test data was in a format which could be used easily by DATAMAP[31]. A second file contained information relating to the channels used, the power supplies and the titles and dates of the files processed. This was very useful in checking that errors did not occur either in the transfer of data or in the data acquisition.

DASPPP contains also plotting routines to display either raw or processed data. These were used for quick inspections of the data.

APPENDIX F

MODEL ROTOR TEST FACILITY IN THE RAE 24ft WIND TUNNEL

F.1 Wind tunnel test facility

The model rotor test facility used for the baseline rectangular blade and the swept tip blades is the RAE 24ft wind tunnel, Fig F1. The wind tunnel is of the closed circuit type with an open working section. Maximum airspeed for the facility is about 50 m/s although this is reduced to about 40 m/s with the safety netting in front of the fan. The test tower is mounted on a mechanical balance which measures both lift and drag. The rotor is positioned at the wind tunnel centre line and the top section of the tower is tilted 5° forward. This angle does not change with the tunnel speed and the tip-path-plane is therefore the same as for a free flight rotor at only one forward speed. The test facility includes also a hover site where the rotor instrumentation can be checked and maintenance undertaken. Also, the rotor can be spun up to a higher rpm than its normal operating speed so that the structural integrity of the rotor can be tested safely. Two video cameras are used to monitor the overall behaviour of the test rig and one camera is zoomed onto the blade tip for tracking purposes.

A minimum of five people is required to run the facility. The test is coordinated from a control room adjacent to the wind tunnel test section. There is extensive safety monitoring equipment in the form of oscilloscopes displaying the loads or bending moments on the hub and blade. One person ensures that the stresses stay within limits. If the stresses do approach their limits, the pilot is warned and the rotor is trimmed to a safer part of its flight envelope. The data acquisition system is controlled by the second team member who has also the responsibility of recording other parameters such as air temperature and pressure. The third person in the control room flies the rotor and therefore has to ensure that the desired flight condition has been reached. The wind tunnel speed is controlled from a separate room adjacent to the test section and the final team member is responsible for the lift and drag readings. There is a direct communication link between the main control room, the wind tunnel control room and the balance room.

F.2 The rotor test rig

The rotor test rig, shown in Fig F2, consists of a tall tower on which the rotor and its control mechanism are mounted. The tower is surrounded by an aerodynamic fairing leaving only the hub and blades exposed to the free-stream airflow. The fairing is not attached to the lift and drag balance and the balance readings will therefore only record the contributions from the rotor. The rotor is powered by four 15 KW water cooled electric motors and the output from the motors is combined by an oil mist lubricated gearbox. Slip rings carry the strain gauge signals from the rotating shaft to the non-rotating tower are mounted around the output shaft of the gearbox.

The cyclic and collective pitch of the blades is controlled by a swash plate mounted below the rotor. Three electric actuators are attached at 120° intervals around the swash plate and they control the tilt and height of the swash plate. This type of arrangement is different from that found on full size helicopters as the movement of any one of the actuators changes the longitudinal cyclic, the collective and possibly also the lateral cyclic. The trimming of the rotor is more difficult therefore especially as the rotor speed cannot be held constant. The system is mechanically simpler however than that found on full size rotors. The position of each actuator is measured using a linear potentiometer and is recorded along with the strain gauge outputs. The signals from the strain gauges are passed through a set of amplifiers (the first stage amplifiers) mounted on top of the hub and passed through the shaft to the slip rings. A slotted disc with an IR transmitter and detector placed either side of it, provides the clock pulse for the data acquisition system. A single slot on one track is used to trigger the start of data recording and hence the blade and hub strains can be related to the azimuthal position of the blades. It is also used to calculate the rotor speed. Another track on the disc with 256 slots, triggers the points in azimuth where the data is to be recorded.

F.3 Data acquisition

The analogue signals from the tower, which include the magnitude of the power supplies, are passed to the control room. The signals pass through a second stage of amplification and some of the more important strain gauge signals, such as those which are expected to reach limits, are displayed on oscilloscopes. The signal from one of the flap gauges is used to trim the rotor. This is usually the flap flexure gauge, although the swept link gauge is used sometimes. The 1Ω component of the signal is minimised by altering the cyclic pitch of the blades. The trim does not have to be exact as the residual 1Ω signal is used as the point to which the loads prediction program is trimmed. Other information displayed in the control room includes rotor speed and torque. The 64 data channels are passed onto a purpose built data acquisition system (DAS) connected to an HP1000 computer. The data is digitised within the DAS and the results are temporarily stored in the computer memory. If a number of revolutions worth of data is required to reduce the effects of wind tunnel turbulence, further sets of data are digitised and added to the existing data. The averaged data is then on a hard disc. Further details of the DAS can be found in Ref 40.

The electronic circuits, in amplifying the signals, introduce a phase change depending on the frequency of the signal. To compensate for this effect, a known signal is passed through the amplifiers prior to the recording of the strain gauge signals. The response to this signal is stored with the other data and by using a fast Fourier transform routine, the strain gauge signals are returned to their proper phasing. The post-processing of the data is explained in more detail in Appendix E.

F.4 Wind tunnel test procedure

The rig is transferred into the wind tunnel test section following the checking of the data links, the operation of the rotor test rig and the overspeed check with the new rotor blades on the hover site. A number of safety nets are installed to contain large

fragments of the rotor within the test section in the event of a blade failure. The tower is temporarily instrumented with accelerometers so that the hub can be dynamically balanced without the blades. The blades are installed and then tracked by strobing the tip of each blade as it passes near the safety video camera mounted at the edge of the test section. Marks on each blade tip help to identify the blade which is not tracking properly. The rotor is stopped and adjustments can be made to the length of the corresponding pitch link. The tracking is repeated until it is acceptable at the normal operating speed.

The collective and cyclic pitch is calibrated next. The positions of the potentiometers which control the swash plate actuators, are presented in arbitrary units and some method is needed to relate the numbers to the blade pitch angles. The three actuator potentiometers are set at various values and the blade pitch angle is measured with an inclinometer. This is repeated with the blade at various positions in azimuth and the relationship between the potentiometer settings and the collective and cyclic pitch angles is determined.

The only parts of the test tower exposed to the airflow are the hub and blades. In the loads prediction programs only the lift from the blades is used in the trimming of the rotor. The lift and drag of the hub is not included in the calculations and therefore its effect has to be taken into account when setting the required thrust on the balance. The lift and drag of the hub is measured for various rotor speed and advance ratio combinations.

At the start of each day of testing or after a change in the rotor configuration, the same resistors are applied to the strain gauge bridges as used in the calibration of the gauges - see section C.2. The voltages are recorded by the DAS and used in the processing of the data to compensate for changes in the supply voltage since the gauge calibration. The rotor is run for a few minutes so as to warm up the gearbox and a valid baseline torque reading can then be recorded. The strain gauge signals are recorded both with the rotor stationary

and operating at 20 rpm ,the latter readings being used as the datum values. The rotor speed is increased to the normal operating value and only then can the wind tunnel speed be increased. The stiffness of the rotor blades in flap is increased by the centrifugal force and an attempt to start the rotor in forward flight can lead to excessive blade deflections (blade sailing) and to possible damage. The wind tunnel speed is slowly increased and the rotor is kept trimmed by altering the cyclic. Having reached the desired advance ratio further trimming is required not only to balance the rotor, but also to achieve the required thrust. The rotor speed is not coupled with the collective pitch mechanism and is therefore another variable in the already complex trim process. The strain gauge data is recorded on reaching the desired thrust, rotor speed and wind tunnel speed. The data is usually averaged over 16 revolutions and following the acquisition of data from a number of flight conditions, a second set of datum readings is taken.

Table 6 shows the flight conditions tested. In previous tests it has been found that at the low thrust of 600N, the maximum load on the flap flexure in the flap direction was exceeded at high advance ratios. Also, for the stall cases where the thrust was slowly increased at a constant advance ratio, the maximum combined flap and lag bending moment on the swept link was exceeded before any signs of retreating blade stall.

F.5 Accuracy of results

There are a number of sources of inaccuracy in the final processed experimental data and the errors can arise from three different areas; the wind tunnel test, data acquisition and data processing.

The first source of error is in the setting up of the flight conditions such as the advance ratio, rotor thrust, rotor speed and rotor trim. The largest error can occur in the value of advance ratio and thrust. The value of both of these variables depends on the wind tunnel temperature and pressure and any changes in these is

compensated for at the beginning of each series of advance ratios for a set thrust - Table 6. For the period during which the data is collected the temperature can vary up to 3 deg which equates to 1% in terms of thrust or advance ratio. The effect of this on blade stresses is very difficult to assess as it depends on how close the blade modal frequency is to a multiple of the forcing frequency. The error in maintaining a given advance ratio is very small, probably less than 0.25%, although the thrust can be measured only to an accuracy of 6N, or 1% for the lowest thrust. The rotor speed is maintained within 1 rev/min of the desired speed (0.3% at the lowest rotation speed) and the rotor cyclic and collective pitch is measured to an accuracy of 0.05 deg.

The signals recorded by the data acquisition system are to a 12 bit accuracy. The accuracy of the final stresses depends on the size of the steady and oscillatory components of the signal. A typical response measured at the blade root is recorded to an accuracy of 0.15% for the oscillatory component although this value will increase for strain gauge stations nearer the tip where the stresses are much lower.

A further source of error is in the processing of the data. The resistors used in place of the strain gauges during the calibrations have a tolerance of 0.1% although this is small compared with the other inaccuracies mentioned above.

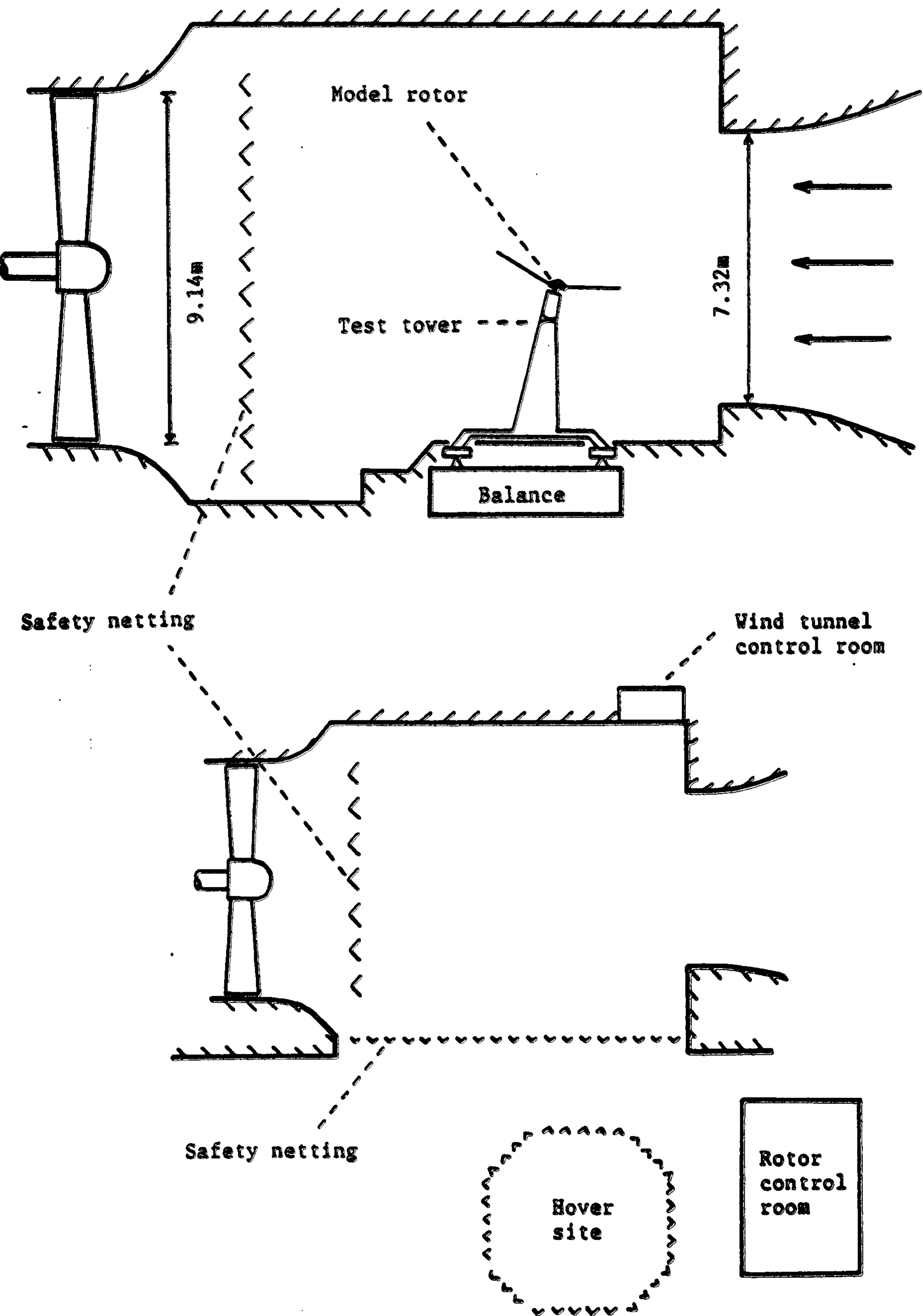


Fig F1 Schematic layout of the model rotor rig in the RAE 24ft wind tunnel

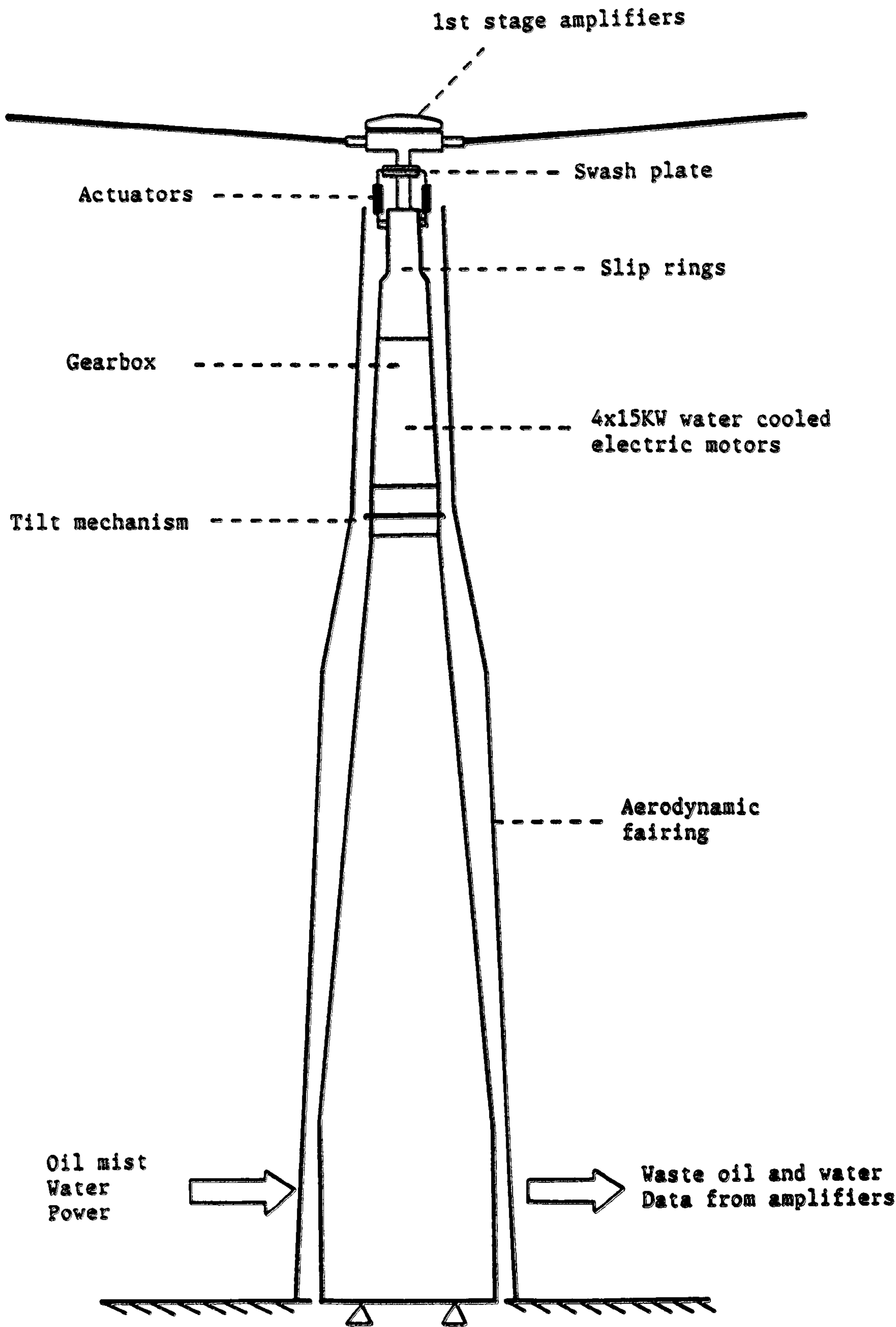


Fig F2 Wind tunnel test rig

APPENDIX G

SWEPT TIP BLADE MK3 - SPECIFICATION AND MANUFACTURE

The details of the design of the Mk3 swept tip blade have been described in Chapter 4. The main difference between this design and the design of the Mk2 blade lies in the layers of composite material on the main section of the blade. The construction of the swept tip and the spar has remained unchanged. Due to the extra fibre layers needed to achieve the sweep in the shear centre, an additional mass was added to the blade leading edge to compensate for the aft shift in the section centre of gravity. The differences between the specifications and manufacturing techniques of the Mk2 and Mk3 blades are described in this Appendix.

G.1. The blade fairing

The construction and manufacture of the blade spar and the foam core have been described in sections D.1 and D.2 respectively. The only similarities between the blade fairing for the Mk3 blade as compared with the Mk2 blade, are the single layer of unidirectional glass fibre covering the entire blade, the 220 gramme/m leading edge weight and the 80 gramme weight at the blade/tip junction.

Fig G1 shows the general position of the layers of glass fibre required to sweep the shear centre by 1.1° . In addition to the sweep of the shear centre, to maintain the same flatwise, edgewise and torsional stiffnesses as for the Mk2 blade, layers of carbon fibre were added above and below the shear centre. The details of these layers are described in the following sections.

G.1.1 Blade root

The number of layers of glass fibre in the design of the Mk2 blade required to move the shear centre onto the quarter chord was reduced towards the spar (Fig D3). For the design of the Mk3 blade the arrangement of these layers had changed so that the spanwise taper of the layers was simpler to manufacture. Fig G2 shows the cross section of the blade marked 'A' in Fig G1. The use of four layers of 913G-E-5-39 unidirectional glass fibre reduced slightly the

torsional stiffness. The $\pm 45^\circ$ carbon fibre strip positioned over the shear centre (labelled '5' in Fig D3) was increased in width from 10mm to 16mm to compensate for this reduced stiffness.

The blade root fitting shown in Fig D2 was machined so the gap between the blade mould and the root fitting accommodated the fibres from the blade fairing. A gap which is too small would cause the fibres to be crushed and resulting in insufficient resin in the pre-preg to bond the fibres to each other and to the root fitting. A gap which is too large could produce voids in the resin therefore reducing the strength of the joint.

G.1.2 Blade mid-section

It was mentioned in section 2.1 that the properties of the model rotor blade were determined by a combination of experimental methods and modelling using finite element techniques. The layers of carbon and glass fibre were linearly tapered along the blade and although the stiffness varies linearly also, this is not true for the radius of gyration term Km_2^2 . A sample of the blade mid-section had been built to check the linearity of the stiffnesses and to determine the value of the inertia.

Fig G3 shows the cross section of the blade at the point marked 'B' in Fig G1. Comparing this figure with the root section, Fig G2, the four layers of glass fibre around the leading edge were reduced in width to achieve the sweep in the shear centre. The removal of the fibres introduced also a reduction in both the flatwise, edgewise and torsional stiffnesses, as well as a rearward shift in the centre of gravity. The flatwise stiffness was increased by the addition of a narrow strip of unidirectional carbon fibre centred 55mm from the leading edge. The sweep of the shear centre was increased by adding glass fibre onto the trailing edge and this helped also in maintaining the edgewise stiffness constant along the blade. Finally, the increase in the width of the two layers of $\pm 45^\circ$ carbon fibre from 16mm to 20.5mm was used to maintain a constant torsional stiffness.

G.1.3 Blade root/tip junction

Fig G4 shows the cross section of the blade at the root/tip junction (section 'C', Fig G1). The additional leading edge glass fibre was replaced with glass fibre at the trailing edge. The layers of carbon fibre centred at 75mm and the trailing edge glass fibres were used to move the shear centre to 56mm from the leading edge.

The leading edge glass fibre in the design of the Mk2 blade had a second role which was to provide reinforcement for the leading edge weight. A thin layer of glass cloth was therefore introduced to the leading edge near the blade/tip junction to reinforce the area around the 80 gramme mass.

G.2 Mass balancing

The sweep of the carbon and glass fibre mentioned above, introduced also an aft shift in the centre of gravity. To retain the same chordwise centre of gravity as for the Mk2 swept tip blade, the leading edge weight was increased. The additional weight linearly tapered from zero at the blade root to 0.228 Kg/m at the blade/tip junction - Fig G5. The weight was omitted however from the point where the 80 gramme mass was positioned.

G.3 Blade properties

Table G1 lists the input data for J146 modes programme. The properties for the sections inboard of the blade root fitting and for the swept tip are the same as for the Mk2 blade - Table 8. The data for the areas where changes had been made are therefore only shown. Test samples of the blade root, mid-span and blade/tip junction had been manufactured and the properties were measured. The blade properties between these points were assumed to be a linear interpolation of the measured values. Linear interpolation cannot be

used for the chordwise radius of gyration Km_2^2 since it is calculated as the mass multiplied by the square of the offset distance in the chordwise direction.

The values of Km_2^2 in the final design stages of the Mk3 blade, described in section 4.1, had to be recalculated to include the new offset between the shear centre and the centre of gravity. New values of inertia were calculated at a number of additional stations along the blade. The continuous line in Fig G6 shows the calculated variation of the inertia with the spanwise distance 'Xs'. The inertia for the inboard and outboard sections of the main part of the blade was measured on the blade samples mentioned above. The values are included as circles on Fig G6. A dashed line is drawn through the points so that it follows the trend of the first line. New values of inertia for the intermediate points were determined.

TABLE G1

J146 INPUT DATA FOR THE MK3 SWEEP TIP BLADE

Xn	Xs (m)	m (Kg/m)	EIf (Nm ²)	EI1 (Nm ²)	EA x E-6 (Nm ² /m ²)	GK x E-1 (Nm ²)	GA x E-6 (Nm ² /m ²)
12	0.4436	3.818	7666.0	33780.0	95.5	821.7	36.73
13	0.4545	2.229	955.0	15935.0	31.6	159.0	12.15
14	0.4876	2.229	955.0	15935.0	31.6	159.0	12.15
15	0.4876	0.614	186.0	6855.0	8.99	6.9	3.458
15A	0.7	#	#	"	"	"	"
15B	0.9	#	#	"	"	"	"
15C	1.1	#	#	"	"	"	"
15D	1.3	#	#	"	"	"	"
16	1.4663	0.849	184.0	6855.0	8.99	6.9	3.458
17	1.4663	1.414	189.0	7725.0	10.01	7.15	3.85
18	1.5663	1.414	189.0	7725.0	10.01	7.15	3.85
19	1.5663	0.849	182.4	6855.0	8.99	6.6	3.458

Xn	β (radians)	Ycg (m)	Yea (m)	Km1 ² xE6 (m ²)	Km2 ² x E6 (m ²)	Zcg (m)	Zea (m)
12	#	"	"	80.3	354.0	"	"
13	0.227	"	"	17.1	286.0	"	"
14	#	"	"	17.1	286.0	"	"
15	#	0.0030	-0.0034	17.75	1425.0	-0.0014	-0.0009
15A	#	#	"	"	1461.0	"	"
15B	#	#	"	"	1520.0	"	"
15C	#	#	"	"	1603.8	"	"
15D	#	#	"	"	1711.0	"	"
16	#	0.0221	"	"	1819.0	"	"
17	#	0.0316	-0.0023	14.77	1674.5	"	"
18	0.1417	0.0333	-0.0023	14.77	1787.8	-0.0014	-0.0009
19	"	0.0238	-0.0034	17.75	1696.7	#	#

'#' denotes that the data are interpolated.

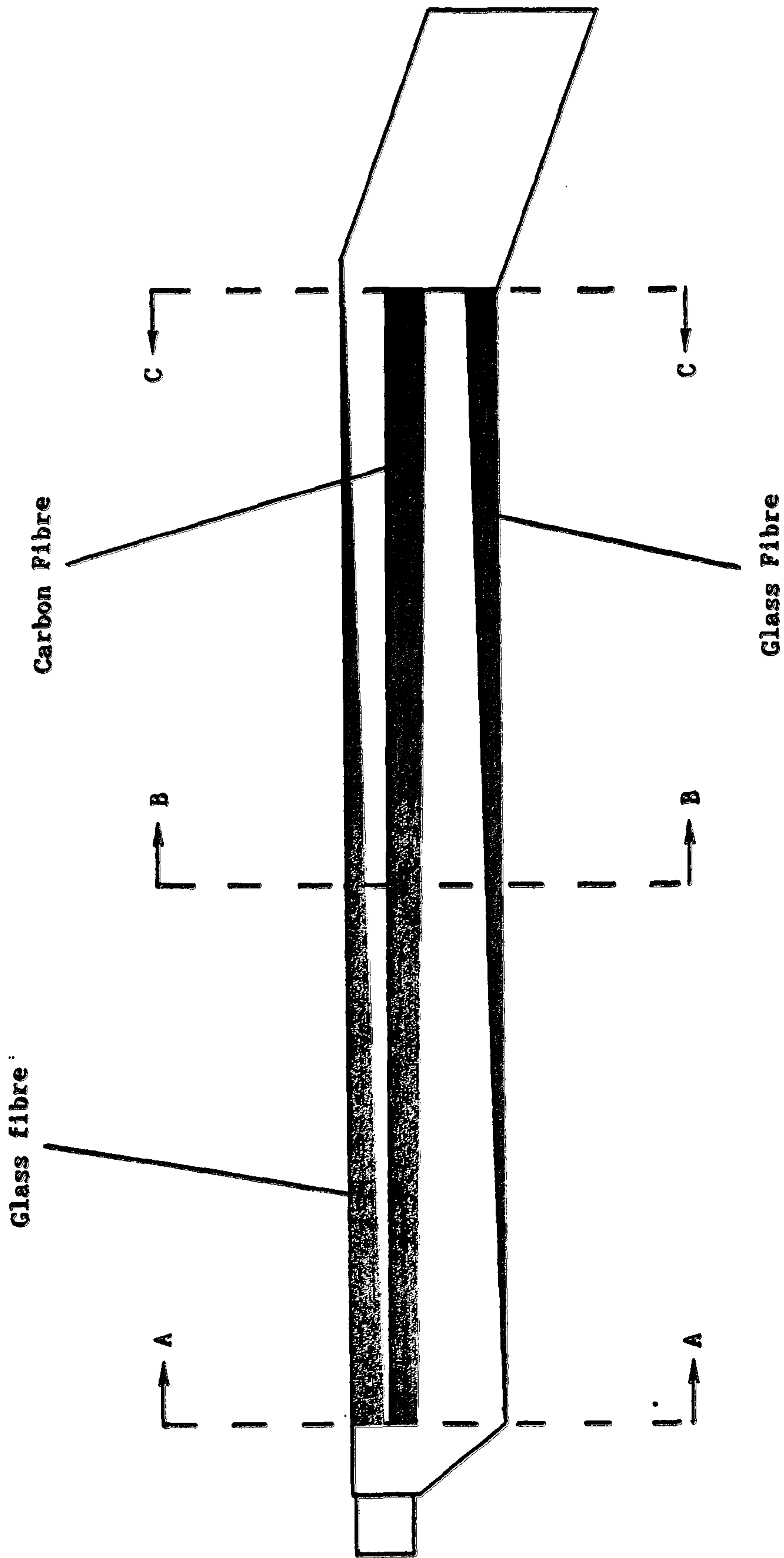


Fig G1 General construction of the Mk3 blade

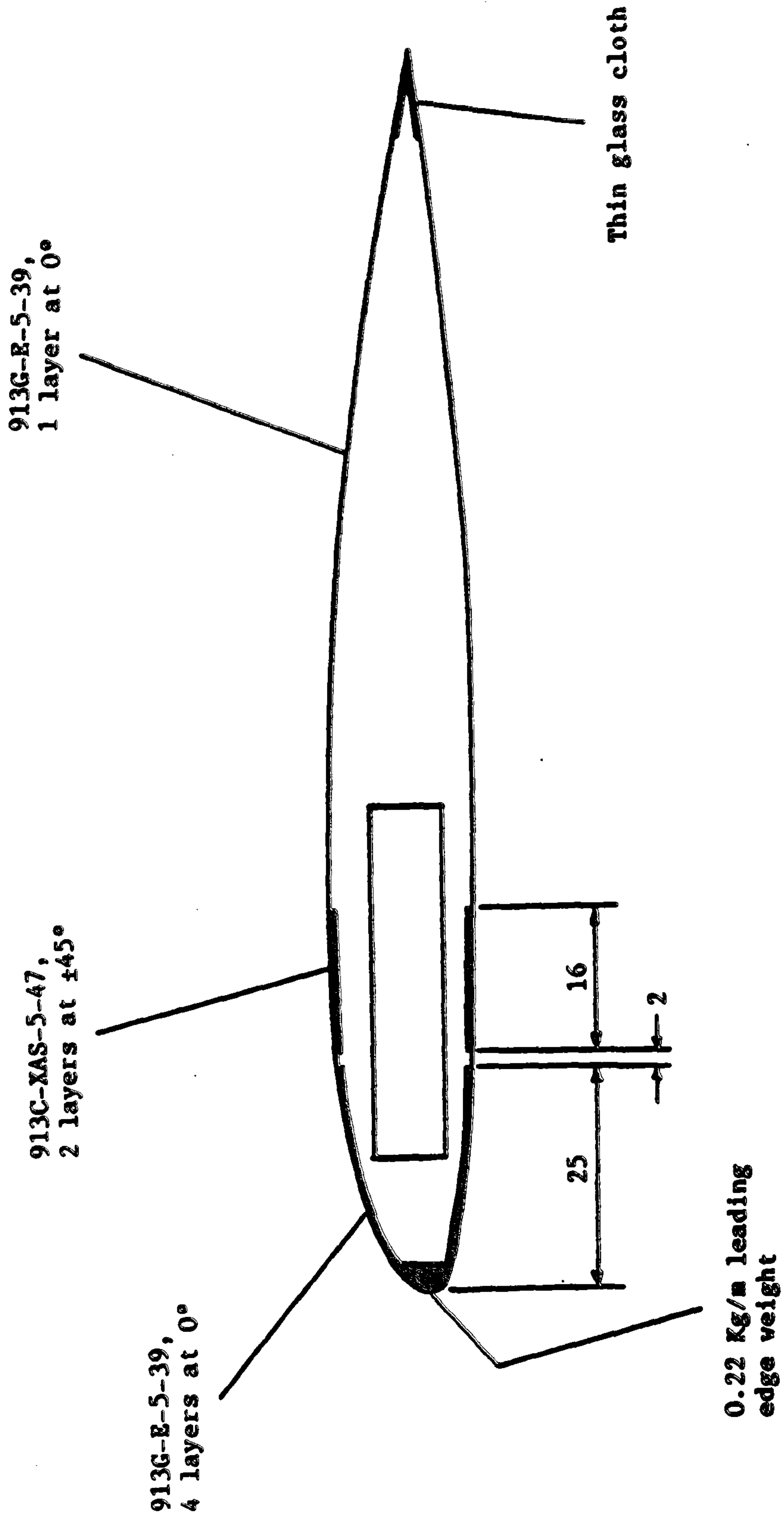


Fig G2 Construction of the Mk3 blade root

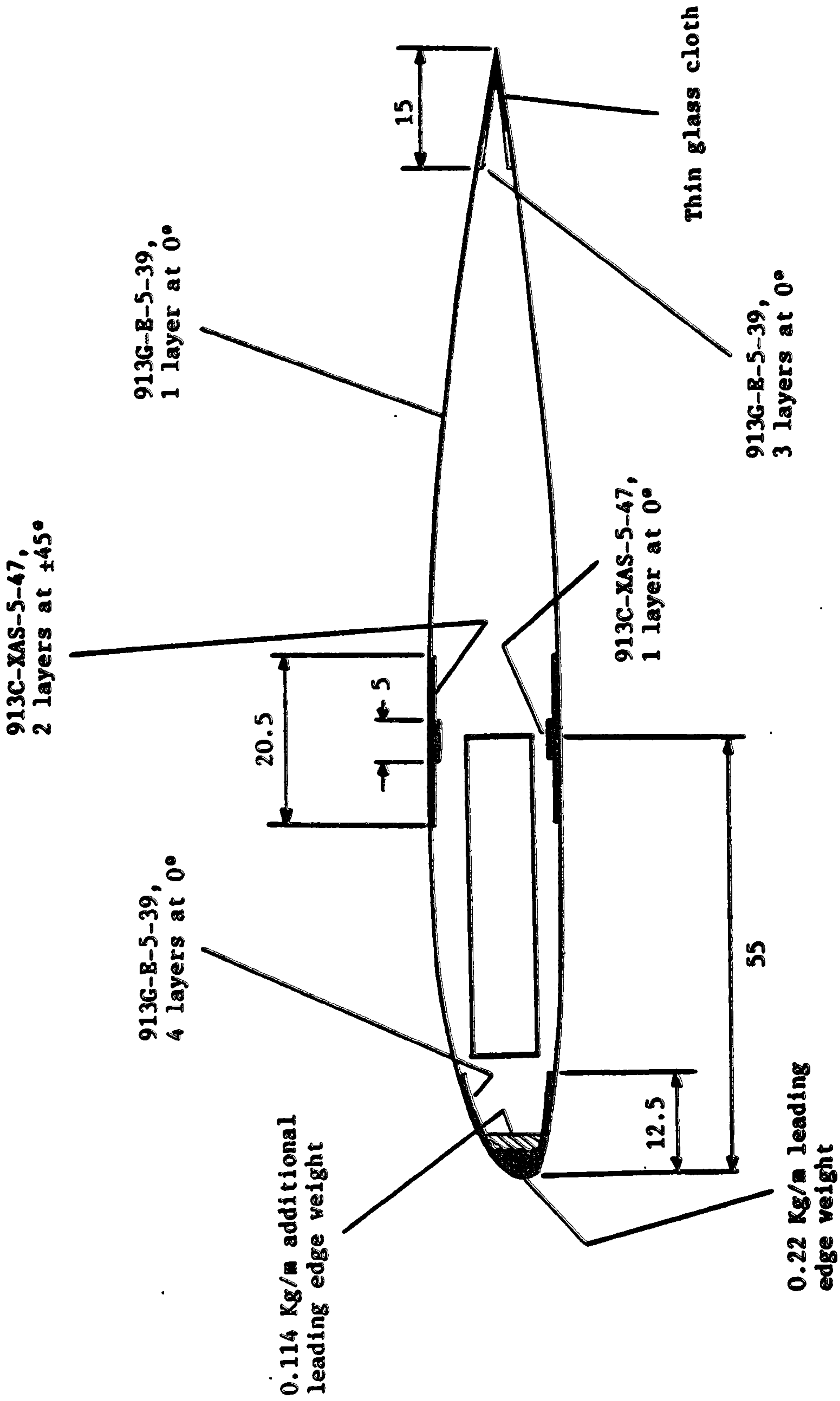


Fig G3 Construction of the mid-section of the Mk3 blade

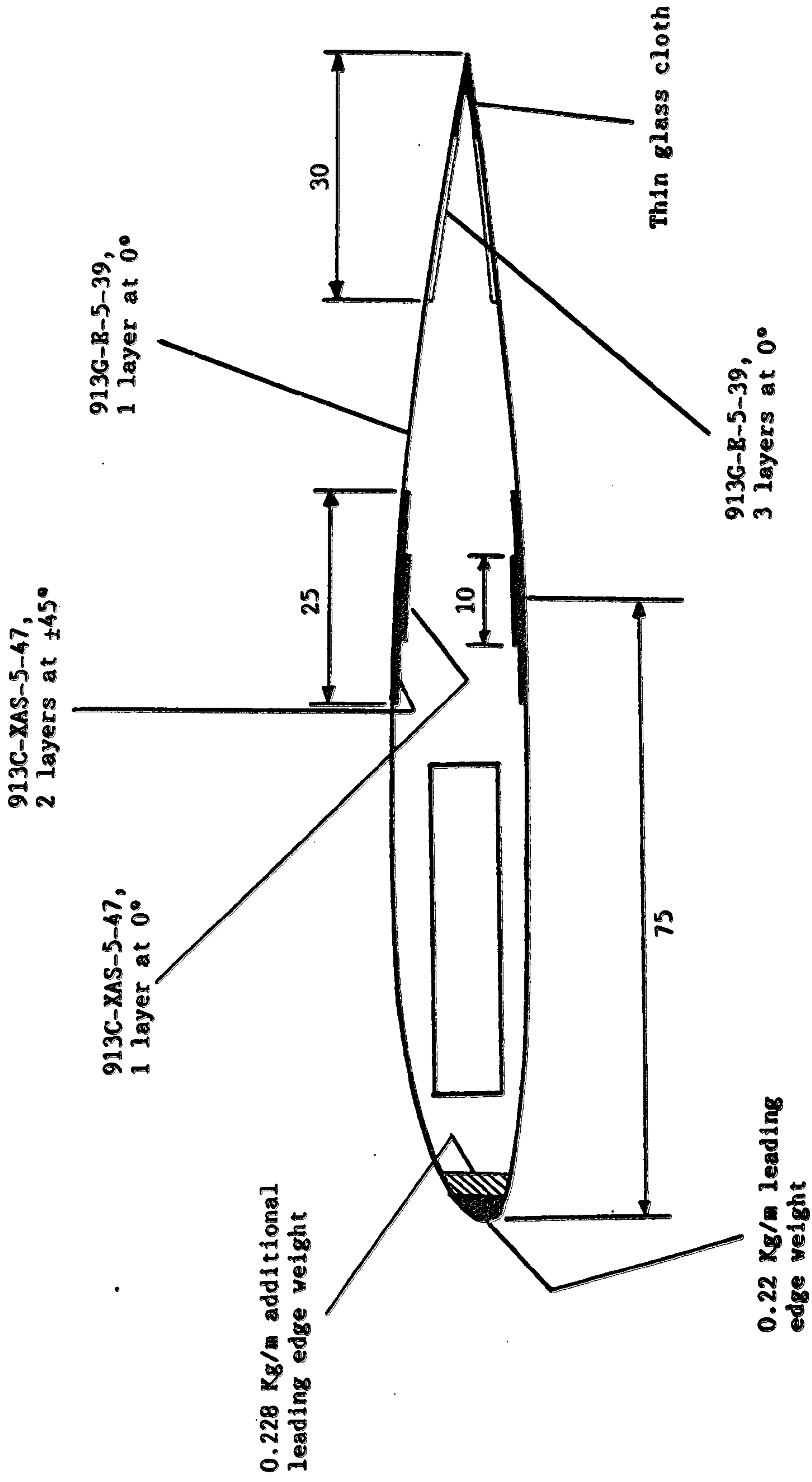


Fig G4 Construction of the Mk3 blade root/tip junction

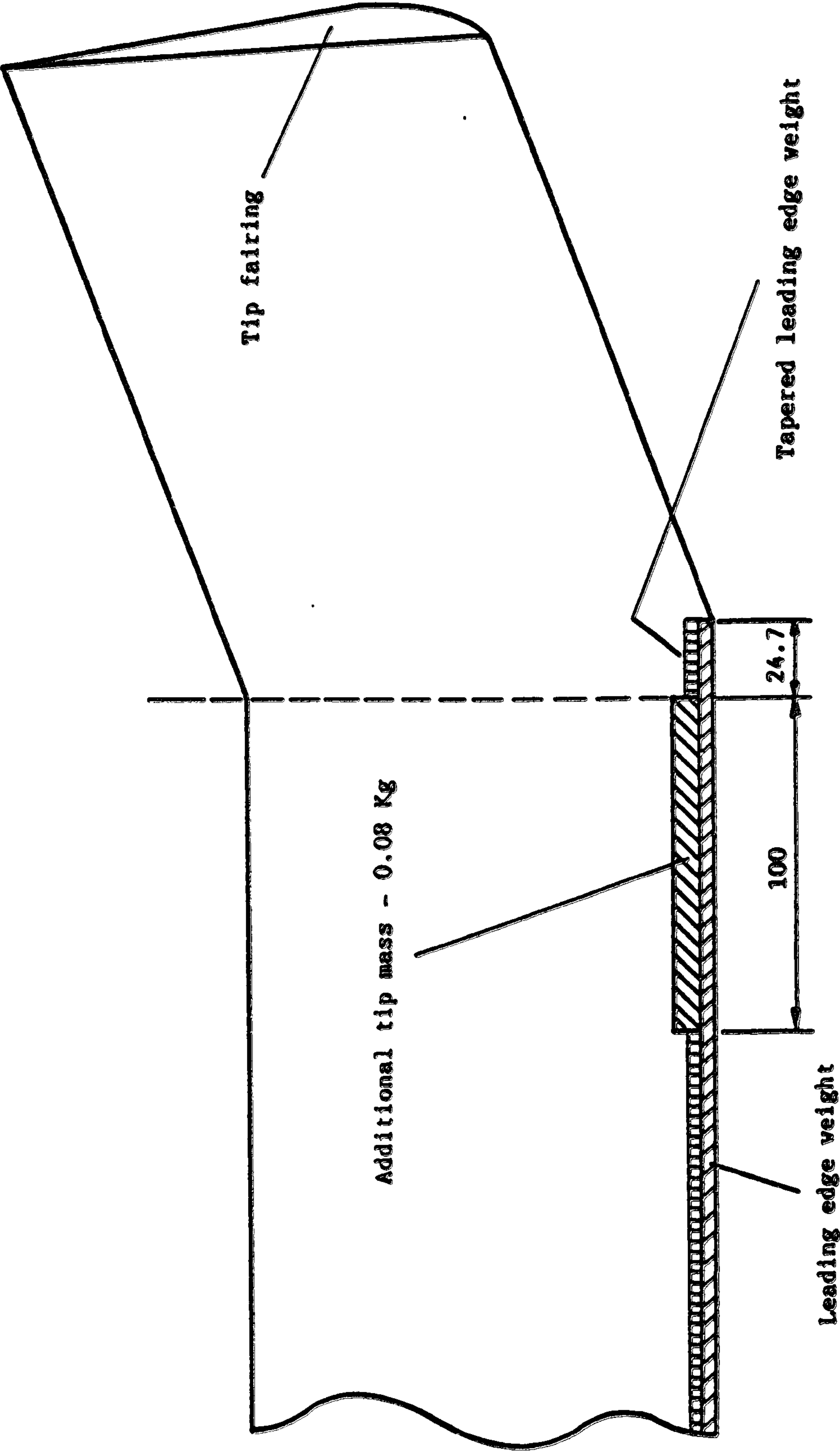


Fig G5 Mass balancing for the Mk3 blade

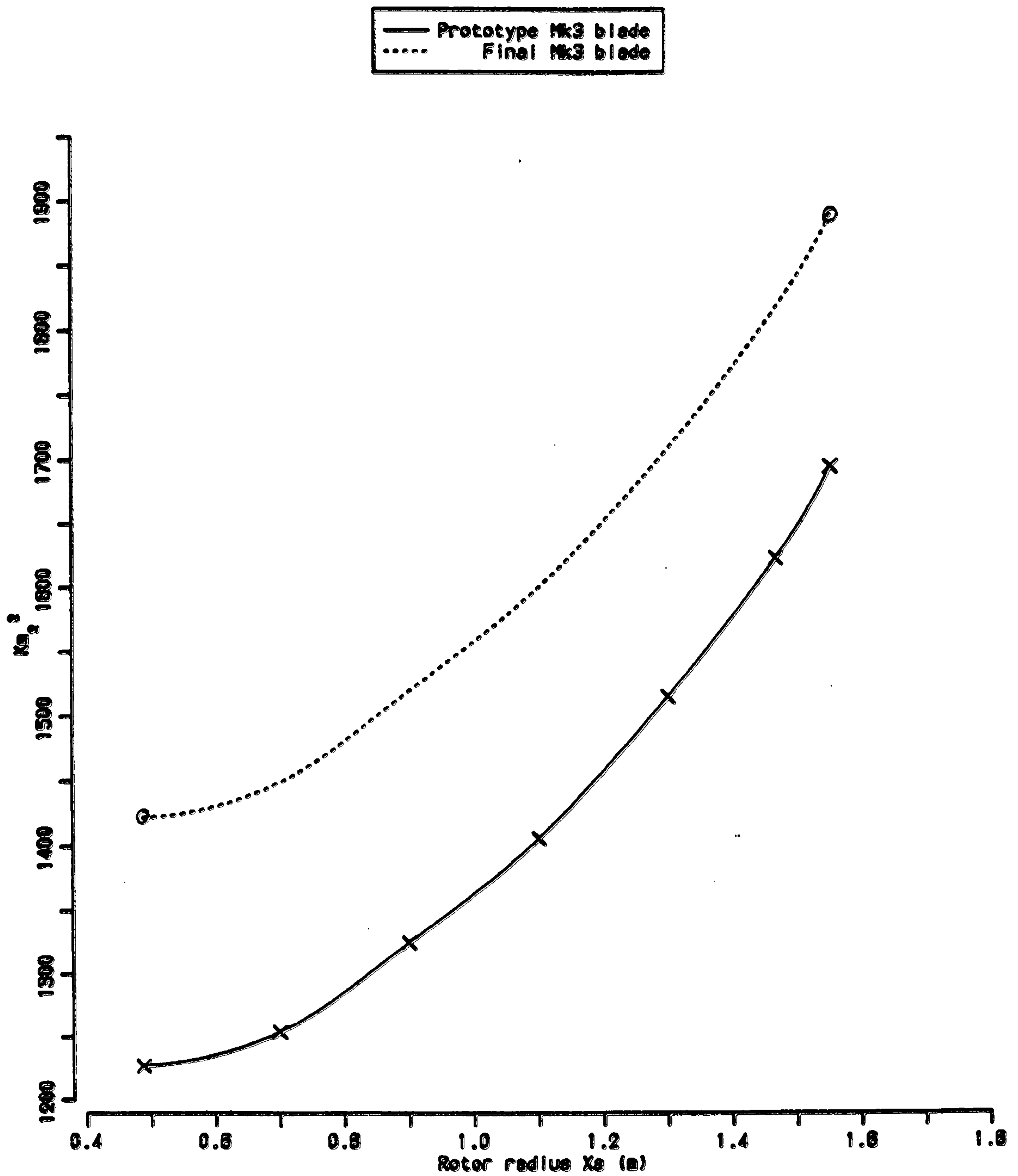


Fig G6 Variation of radius of gyration (Km_2^2) along the Mk3 blade

TABLE 1

RAE 9642 AEROFOIL COORDINATES

Distance from leading edge (mm)	Upper surface (mm)	Lower surface (mm)
0.0000	0.0000	0.0000
0.0700	0.5670	-0.3878
0.1400	0.8092	-0.5390
0.2240	1.0346	-0.6664
0.3374	1.2670	-0.7966
0.7000	1.8424	-1.1088
1.3454	2.6306	-1.5218
2.1000	3.3236	-1.8802
3.0142	4.0222	-2.1966
4.2000	4.7670	-2.4934
5.3284	5.3494	-2.7076
8.2656	6.5744	-3.1332
11.7980	7.7252	-3.5406
15.8890	8.7766	-3.9298
20.5030	9.6376	-4.2938
25.5920	10.1300	-4.6340
31.1090	10.4170	-4.9476
37.0020	10.5760	-5.2248
43.2120	10.6820	-5.4558
49.6800	10.6750	-5.6308
56.3430	10.5480	-5.7372
63.1390	10.2960	-5.6910
70.0000	9.9176	-5.5160
76.8610	9.5298	-5.2206
83.6570	8.9278	-4.8258
90.3200	8.2012	-4.3862
96.7880	7.3808	-3.8878
103.0000	6.4862	-3.3656
108.8900	5.5482	-2.8392
114.4100	4.6102	-2.3254
119.5000	3.7100	-1.8634
124.1100	2.8756	-1.4435
128.2000	2.1350	-1.0726
131.7300	1.5246	-0.7517
134.6700	0.9826	-0.4844
136.9900	0.5548	-0.2736
140.0000	0.0000	0.0000

TABLE 2

STRUCTURAL PROPERTIES OF THE PROTOTYPE MK1
SWEPT TIP BLADE

		<u>Inboard section</u>	<u>Tip section</u>
Mass/unit length	Kg/m	0.631	0.373
Flatwise stiffness	Nm ²	177	178
Edgewise stiffness	Nm ²	7130	5800
Torsional stiffness	Nm ²	56	145
Product of Young's modulus and cross-sectional area	N/m ² .m ²	4.015E6	4.015E6
Flatwise radius of gyration squared	m ²	16.2E-6	24.18E-6
Edgewise radius of gyration squared	m ²	1454.E-6	1246.E-6
C.g. position from l.e.		0.252c	0.4c
Shear centre position from l.e.		0.257c	0.336c
Elastic axis position from l.e.		0.277c	0.336c

TABLE 3

RADIAL POSITION OF BLADE AND HUB COMPONENTS - J134

Xn	Xs(m)	
1	0.09713	Elastomeric bearing
2	0.1001	
3	0.1001	Yoke
4	0.18913	
5	0.18913	Tension member
6	0.20353	
7	0.20353	Coupling bearing support frame
8	0.23773	
9	0.23773	Tension member
10	0.24853	
11	0.27373	Tension member/swept link joint
12	0.29713	
13	0.29713	
14	0.31873	Swept link
15	0.36913	
16	0.4063	
17	0.4063	Swept link/blade root fitting joint
18	0.4308	
19	0.4308	
20	0.4473	Blade root fitting
21	0.4573	
22	0.4879	
23	0.4879	Blade
24	1.7819	
25	1.7819	Blade and aluminium block
26	1.7989	
27	1.7989	
28	1.8019	Blade tip

TABLE 4

J134 INPUT DATA FOR THE BASELINE RECTANGULAR BLADE

Xn	Xs (m)	m (Kg/m)	EIf (Nm ²)	EI1 (Nm ²)	EA x E-6 (Nm ² /m ²)
1	0.09713	0.0201	0.0936	0.0936	0.47
2	0.1001	0.0201	0.0936	0.0936	0.47
3	0.1001	5.0	10500.0	78400.0	112.0
4	0.18913	5.0	10500.0	78400.0	112.0
5	0.18913	3.5	665.0	665.0	41.6
6	0.20353	3.5	665.0	665.0	41.6
7	0.20353	27.3	1030.0	1030.0	51.6
8	0.23773	27.3	1030.0	1030.0	51.6
9	0.23773	1.62	665.0	665.0	41.6
10	0.24853	1.62	665.0	665.0	41.6
11	0.27373	4.07	5500.0	29000.0	104.0
12	0.29713	4.07	5500.0	29000.0	104.0
13	0.29713	2.03	686.0	14500.0	51.2
14	0.31873	0.8	251.0	924.0	20.16
15	0.36913	0.8	251.0	924.0	20.16
16	0.4063	2.03	686.0	14500.0	51.2
17	0.4063	4.07	5500.0	29000.0	104.0
18	0.4308	4.07	5500.0	29000.0	104.0
19	0.4308	3.818	7666.0	33780.0	95.5
20	0.4473	3.818	7666.0	33780.0	95.5
21	0.4573	2.229	955.0	15935.0	31.6
22	0.4879	2.229	955.0	15935.0	31.6
23	0.4879	0.631	177.0	7130.0	8.817
24	1.7819	0.631	"	"	"
25	1.7819	1.1219	"	"	"
26	1.7989	1.1219	"	"	"
27	1.7989	0.4909	"	"	"
28	1.8019	0.4909	"	"	"

TABLE 4 cont.

Xn	GK x E-1 (Nm ²)	β (radians)	Ycg (m)	Yea (m)	Km1 ² xE6 (m ²)	Km2 ² x E6 (m ²)
1	0.00198	0.0	0.0	0.0	0.201	0.201
2	0.00198	"	"	"	0.201	0.201
3	4150.0	"	"	"	93.6	700.0
4	4150.0	"	"	"	93.6	700.0
5	53.2	"	"	"	16.0	16.0
6	53.2	"	"	"	16.0	16.0
7	82.0	"	"	"	19.8	19.8
8	82.0	"	"	"	19.8	19.8
9	53.2	"	"	"	16.0	16.0
10	53.2	"	"	"	16.0	16.0
11	660.0	"	"	"	53.7	284.0
12	660.0	"	"	"	53.7	284.0
13	94.3	"	"	"	13.4	"
14	32.7	"	"	"	12.45	45.83
15	32.7	"	"	"	12.45	45.83
16	94.3	"	"	"	13.4	284.0
17	660.0	"	"	"	53.7	284.0
18	660.0	"	"	"	53.7	272.0
19	821.7	"	"	"	80.3	354.0
20	821.7	"	"	"	80.3	354.0
21	159.0	0.0	"	"	17.1	286.0
22	159.0	#	"	"	17.1	286.0
23	5.6	#	-0.0006	-0.0038	16.2	1454.4
24	"	#	"	"	"	"
25	"	#	"	"	"	"
26	"	#	"	"	"	"
27	"	#	"	"	"	"
28	"	-0.1038	"	"	"	"

'#' denotes that the data are interpolated.

Xn	Zcg (m)	Zea (m)
1	0.0	0.0
22	0.0	0.0
23	-0.0014	-0.0009
28	-0.0014	-0.0009

Table 5

STRUCTURAL PROPERTIES OF THE MK2
SWEPT TIP BLADE

		<u>27-82%R</u>	<u>82-88%R</u>	<u>88-100%R</u>
Mass/unit length	Kg/m	0.614	1.414	0.364
Flatwise stiffness	Nm ²	182.4	189.4	178
Edgewise stiffness	Nm ²	6855	7725	5800
Torsional stiffness	Nm ²	66	71.5	145
Product of Young's modulus and cross-sectional area	N/m ² .m ²	8.99E6	10.01E6	8.5E6
Flatwise radius of gyration squared	m ²	17.75E-6	14.77E-6	24.18E-6
Edgewise radius of gyration squared	m ²	1229.5E-6	887.7E-6	1246.E-6
C.g. position from l.e.		0.229c	0.132c	0.4c
Shear centre position from l.e.		0.251c	0.236c	0.336c
Elastic axis position from l.e.		0.275c	0.252c	0.336c

TABLE 6

WIND TUNNEL TEST CONDITIONS

Thrust, drag, and torque tares:-

- (a) 20 and 600 rev/min at $\mu = 0$
- (b) $\mu = 0.1, 0.12, \dots, 0.34$ at 600 rev/min
- (c) $\mu = 0, 0.1, 0.2, 0.3, 0.34$ at 300, 400, 500, 550 rev/min

Tests with swept tip blades:-

- (d) $\mu = 0.1, 0.12, \dots, 0.34$ at 600 rev/min
Thrust = 600N, 750N, 900N and 1050N
- (e) $\mu = 0.1, 0.2, 0.3, 0.34$ at 600 rev/min
Thrusts starting at 1050N until rotor limits reached.
- (f) $\mu = 0.1, 0.2, 0.3, 0.34$ at 300, 400, 500, 550 rev/min
Thrust coefficients as for test (d).

TABLE 7

J134 INPUT DATA FOR THE MK2 SWEEPED TIP BLADE

Xn	Xs (m)	m (Kg/m)	EIF (Nm ²)	EI1 (Nm ²)	EA xE-6 (Nm ² /m ²)	GK x E-1 (Nm ²)	β radians
21	0.4573	2.229	955.0	15935.0	31.6	159.0	0.0687
22	0.4879	2.229	955.0	15935.0	31.6	159.0	#
23	0.4879	0.614	182.4	6855.0	8.99	6.6	#
24	1.4660	0.614	182.4	6855.0	8.99	6.6	#
25	1.4660	1.414	189.0	7725.0	10.01	7.15	#
26	1.5660	1.414	189.0	7725.0	10.01	7.15	-0.0166
27	1.5660	0.614	182.4	6855.0	8.99	6.6	"
28	1.6024	0.364	178.0	5800.0	8.5	14.15	"
29	1.6024	0.364	"	"	"	"	"
30	1.7976	0.364	"	"	"	"	"
31	1.7976	0.896	"	"	"	"	"
32	1.8146	0.896	"	"	"	"	"
33	1.8146	0.532	"	"	"	"	"
34	1.8176	0.532	"	"	"	"	-0.0166

'#' denotes that the data are interpolated

Xn	Ycg (m)	Yea (m)	Km1 ² xE6 (m ²)	Km2 ² xE6 (m ²)	Zcg (m)	Zea (m)
21	0.0	0.0	17.1	286.0	0.0	0.0
22	0.0	0.0	17.1	286.0	0.0	0.0
23	0.003	-0.0034	17.75	1229.5	-0.0014	-0.0009
24	0.003	-0.0034	17.75	1229.5	"	"
25	0.0145	-0.0023	14.77	887.7	"	"
26	0.0145	-0.0023	14.77	887.7	-0.0014	-0.0009
27	0.003	-0.0034	17.75	1229.5	0.0	0.0
28	-0.009	0.005	24.18	1246.0	0.0	0.0
34	-0.009	0.005	24.18	1246.0	0.0	0.0

TABLE 8**J146 INPUT DATA FOR THE MK2 SWEEP TIP BLADE**

Xn	Xs (m)	m (Kg/m)	EIf (Nm²)	EIl (Nm²)	EA x E-6 (Nm²/m²)	GK x E-1 (Nm²)	GA x E-6 (Nm²/m²)
1	0.2395	1.62	665.0	665.0	41.6	53.2	16.0
2	0.2510	1.62	665.0	665.0	41.6	53.2	16.0
3	0.2697	4.07	5500.0	29000.0	104.0	660.0	40.0
4	0.2940	4.07	5500.0	29000.0	104.0	660.0	40.0
5	0.2940	2.03	686.0	14500.0	51.2	94.3	19.69
6	0.3203	0.8	251.0	924.0	20.16	32.7	7.754
7	0.3703	0.8	251.0	924.0	20.16	32.7	7.754
8	0.4038	2.03	686.0	14500.0	51.2	94.3	19.69
9	0.4038	4.07	5500.0	29000.0	104.0	660.0	40.0
10	0.4286	4.07	5500.0	29000.0	104.0	660.0	40.0
11	0.4286	3.818	7666.0	33780.0	95.5	821.7	36.73
12	0.4436	3.818	7666.0	33780.0	95.5	821.7	36.73
13	0.4545	2.229	955.0	15935.0	31.6	159.0	12.15
14	0.4876	2.229	955.0	15935.0	31.6	159.0	12.15
15	0.4876	0.614	182.4	6855.0	8.99	6.6	3.458
16	1.4660	0.614	182.4	6855.0	8.99	6.6	3.458
17	1.4660	1.414	189.0	7725.0	10.01	7.15	3.85
18	1.5660	1.414	189.0	7725.0	10.01	7.15	3.85
19	1.5660	0.614	182.4	6855.0	8.99	6.6	3.458
20	1.6024	0.364	178.0	5800.0	8.5	14.5	3.269
21	1.6024	0.364	178.0	5800.0	8.5	14.5	3.269
22	1.7976	0.364	"	"	"	"	"
23	1.7976	0.896	"	"	"	"	"
24	1.8146	0.896	"	"	"	"	"
25	1.8146	0.532	"	"	"	"	"
26	1.8176	0.532	178.0	5800.0	8.5	14.5	3.269

TABLE 8 cont.

Xn	β (radians)	Ycg (m)	Yea (m)	Km1 ² xE6 (m ²)	Km2 ² x E6 (m ²)	Zcg (m)	Zea (m)
1	0.0	0.0	0.0	16.0	16.0	0.0	0.0
2	#	"	"	16.0	16.0	"	"
3	#	"	"	53.7	284.0	"	"
4	#	"	"	53.7	284.0	"	"
5	#	"	"	13.4	#	"	"
6	#	"	"	12.45	45.83	"	"
7	#	"	"	12.45	45.83	"	"
8	#	"	"	13.4	284.0	"	"
9	#	"	"	53.7	284.0	"	"
10	#	"	"	53.7	284.0	"	"
11	#	"	"	80.3	354.0	"	"
12	#	"	"	80.3	354.0	"	"
13	0.227	"	"	17.1	286.0	"	"
14	#	"	"	17.1	286.0	"	"
15	#	0.0030	-0.0034	17.75	1229.5	-0.0014	-0.0009
16	#	"	"	17.75	1229.5	"	"
17	#	0.0145	-0.0023	14.77	887.7	"	"
18	0.1417	0.0145	-0.0023	14.77	887.7	-0.0014	-0.0009
19	"	0.003	-0.0034	17.75	1229.5	#	#
20	"	-0.009	0.005	24.18	1246.0	0.0	0.0
21	"	"	"	24.18	1246.0	"	"
22	"	"	"	"	"	"	"
23	"	"	"	"	"	"	"
24	"	"	"	"	"	"	"
25	"	"	"	"	"	"	"
26	"	"	"	"	"	"	"

'#' denotes that the data are interpolated.

TABLE 9

STRUCTURAL PROPERTIES OF FLEXURE 1

Xn	Xs (m)	m (Kg/m)	EIf (Nm ²)	EI1 (Nm ²)	EA x E-6 (Nm ² /m ²)
1	0.085	0.824	63.11	84727.0	21.43
2	0.1155	0.71	41.05	73477.0	18.47
3	0.146	0.602	24.83	62085.0	15.65
4	0.1765	0.493	13.575	50836.0	12.82
5	0.207	0.383	6.368	39444.0	9.955
6	0.207	19.803	313959.0	313959.0	515.0
7	0.237	"	"	"	"
8	0.237	"	"	"	"
9	0.2395	"	"	"	"

Xn	GK (Nm ²)	GA x E-6 (Nm ² /m ²)	Km1 ² x E6 (m ²)	Km2 ² xE6 (m ²)
1	70.5	8.242	5.9	3960.0
2	"	7.104	4.438	"
3	"	6.019	3.168	"
4	"	4.931	2.124	"
5	"	3.829	1.279	"
6	174659.0	198.1	609.0	609.0
7	"	"	"	"
8	"	182.5	"	"
9	"	"	"	"

TABLE 10

STRUCTURAL PROPERTIES OF FLEXURE 2

Xn	Xs (m)	m (Kg/m)	EIf (Nm ²)	EIl (Nm ²)	EA x E-6 (Nm ² /m ²)
1	0.0935	13.95	19137.0	154349.0	367.43
2	0.1055	13.95	19137.0	154349.0	367.43
3	0.1055	3.144	4312.5	82600.0	82.8
4	0.1625	3.144	4312.5	82600.0	82.8
5	0.1625	13.95	19137.0	154349.0	367.43
6	0.1745	13.95	19137.0	154349.0	367.43
7	0.1745	1.62	665.0	665.0	41.6
8	0.1955	1.62	"	"	"
9	0.1955	5.64	"	"	"
10	0.2395	5.64	"	"	"

Xn	GK (Nm ²)	GA x E-6 (Nm ² /m ²)	Km1 ² x E6 (m ²)	Km2 ² x E6 (m ²)
1	23884.0	141.3	52.08	420.08
2	23884.0	141.3	52.08	420.08
3	4517.0	31.85	104.17	3051.2
4	4517.0	31.85	104.17	3051.2
5	23884.0	141.3	52.08	420.08
6	23884.0	141.3	52.08	420.08
7	53.2	16.0	16.0	16.0
8	"	"	16.0	16.0
9	"	"	4.6	4.6
10	"	"	4.6	4.6

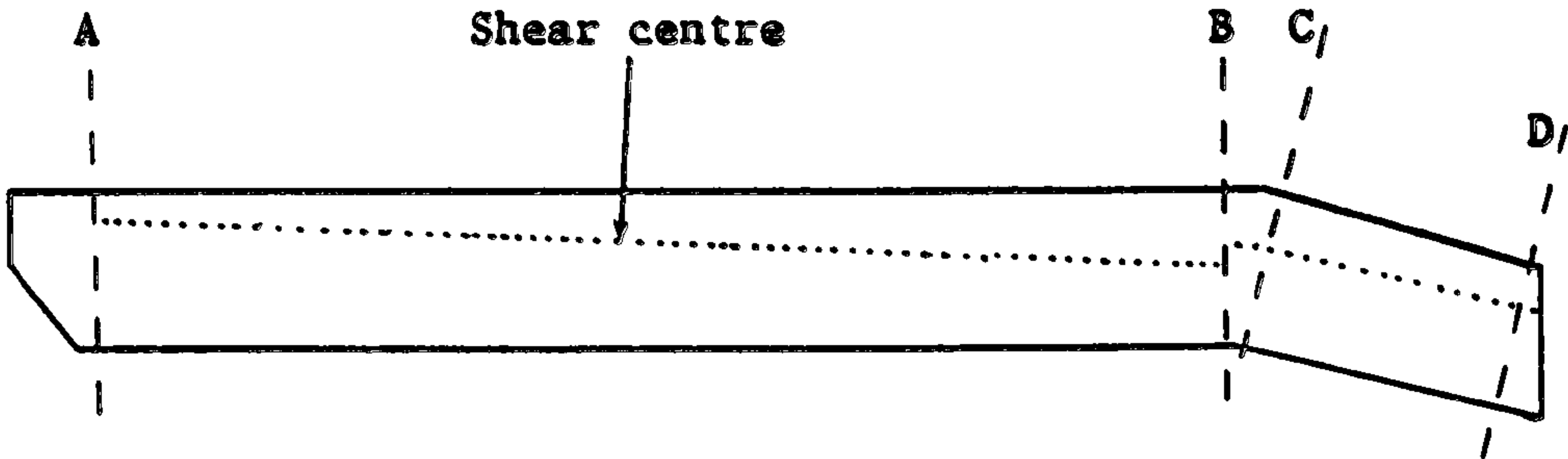
TABLE 11

RADIAL POSITION OF BLADE AND HUB COMPONENTS - J146

	Xn	Xs(m)	
F L E X U R E 1	1	0.085	
	2	0.1155	Flap flexure
	3	0.146	
	4	0.1765	
	5	0.207	
	6	0.207	
	7	0.237	Coupling bearing
	8	0.237	support frame
	9	0.2395	
F L E X U R E 2	1	0.0935	Inner yoke section
	2	0.1055	
	3	0.1055	Mid yoke section
	4	0.1625	
	5	0.1625	Outer yoke section
	6	0.1745	
	7	0.1745	Tension member
	8	0.1955	
	9	0.1955	Tension member +
	10	0.2395	coupling bearing
B L A D E S E C T I O N	1	0.2395	Tension member
	2	0.2510	
	3	0.2697	Tension member/swept link joint
	4	0.2940	
	5	0.2940	
	6	0.3203	Swept link
	7	0.3703	
	8	0.4038	
	9	0.4038	Swept link/blade root fitting joint
	10	0.4286	
	11	0.4286	
	12	0.4436	Blade root fitting
	13	0.4545	
	14	0.4876	
	15	0.4876	Blade inboard section
	16	1.4660	
	17	1.4660	Inboard section with increased
	18	1.5660	leading edge mass
	19	1.5660	Blade/tip junction
	20	1.6024	
	21	1.6024	Swept tip
	22	1.7976	
	23	1.7976	Swept tip + aluminium block
	24	1.8146	
	25	1.8146	
	26	1.8176	Swept tip

TABLE 12

PROVISIONAL PROPERTIES FOR THE MK3 SWEEP TIP BLADE



			A	B	C	D
m	-	(Kg/m)	0.614	0.614	0.364	0.364
EI_f	-	(Nm ²)	182.4	182.4	178.0	178.0
EI_1	-	(Nm ²)	6855	6855	5800	5800
GJ	-	(Nm ²)	66	66	145	145
EA	-	([N/m ²].m ²)	8.99E6	8.99E6	8.5E6	8.5E6
Km_1^2	-	(m ²)	17.75E-6	17.75E-6	24.18E-6	24.18E-6
Km_2^2	-	(m ²)	1229.5E-6	1697.0E-6	1457.4E-6	1814.0E-6
Y_{cg}	-	(mm from le)	32.1	32.1	56.0	56.0
Y_{sc}	-	(mm from le)	35.1	54.0	39.0	30.0
Y_{ea}	-	(mm from le)	38.5	57.4	39.0	30.0

TABLE 13

EXPERIMENTALLY DERIVED PROPERTIES FOR THE MK3 BLADE

	SECTION A		SECTION B	
	REQUIRED	MEASURED	REQUIRED	MEASURED
EIF (Nm ²)	182.4	186	182	184
GJ (Nm ²)	66	69	66	69
Ysc (mm)	35.1	35	54	56
Km2 ² (m ²)	1229E-6	1425.0E-6	1697.0E-6	1895.0E-6
m (Kg/m)	0.614	0.606	0.614	0.841

	SECTION C		MID SECTION A-B	
	REQUIRED	MEASURED	REQUIRED	MEASURED
EIF (Nm ²)	178	172	182	171
GJ (Nm ²)	145	157	66	64
Ysc (mm)	38	39	45	46
Km2 ² (m ²)	-	1830.3E-6	-	-
m (Kg/m)	0.364	0.486	-	-

(See Table 12 for definition of sections A, B and C).

TABLE 14

PROPERTIES FOR THE PUMA BLADE

Xs (m)	m (Kg/m)	EIF x E-4 (Nm²)	EI1 x E-5 (Nm²)	EA x E-8 (Nm²/m²)	GJ x E-4 (Nm²)
1.25002	-	-	15.29901	3.7443	8.5
1.26	7.15	8.1	#	3.7443	"
1.26	"	"	#	1.6975	"
1.697	"	"	#	1.6884	"
1.697	"	"	#	1.6884	"
1.757	"	"	#	1.6534	"
1.77	7.15	"	#	#	"
1.880	#	"	#	1.7084	"
1.887	8.929	"	#	#	"
2.128	"	"	#	1.701	"
2.278	"	"	#	1.5099	"
5.250	"	"	#	1.4504	"
5.8	"	"	#	1.4392	"
6.11	"	"	#	1.4294	"
7.0695	8.929	"	#	"	"
7.0695	12.7535	"	#	"	"
7.2414	"	"	#	"	8.5
7.2414	"	"	#	"	8.7013
7.30002	"	8.1	14.40075	"	"
7.31	"	8.19907	7.14872	"	"
7.35	"	"	"	1.4294	"
7.35	"	"	"	1.6093	"
7.382	"	"	"	1.6093	"
7.382	"	"	"	1.1557	"
7.3895	12.7535	"	"	"	"
7.3895	34.6	"	"	"	"
7.4022	34.6	"	"	"	"
7.4022	6.93	"	"	"	"
7.5	6.93	8.19907	7.14872	1.1557	8.7013

'#' denotes that the data are interpolated.

TABLE 14 cont.

PROPERTIES FOR THE PUMA BLADE

Xs (m)	β (degrees)	Ycg (m)	Km1 ² x E3 (Nm ²)	Km2 ² x E3 (Nm ² /m ²)
0.6	-0.64	0.0	-	-
1.0851	#	"	0.26942	1.64374
1.25002	#	"	#	#
1.26	#	"	#	#
1.26	#	"	#	#
1.697	#	"	#	#
1.697	#	"	#	#
1.757	#	"	#	#
1.77	#	"	0.4955	5.74973
1.880	#	"	"	#
1.887	#	0.0	"	#
1.887	#	-0.00533	"	#
2.128	#	"	"	#
2.278	#	"	"	#
5.250	#	"	"	#
5.8	#	"	"	#
6.11	#	"	"	#
7.0695	#	-0.00533	"	#
7.0695	#	0.01599	"	#
7.2414	#	"	"	#
7.2414	#	"	"	#
7.30002	#	"	"	#
7.31	#	"	"	#
7.35	#	"	"	#
7.35	#	"	"	#
7.382	#	"	"	#
7.382	#	"	"	#
7.3895	#	0.01599	0.4955	11.74
7.3895	#	0.07888	0.1314	9.6
7.4022	#	0.07888	"	"
7.4022	#	0.0	"	"
7.434	#	"	0.1314	9.6
7.434	#	"	0.5962	11.65
7.5	-8.0	0.0	0.5962	11.65

'#' denotes that the data are interpolated.

TABLE 15**PERFORMANCE OF THE BASELINE PUMA BLADE**

Advance ratio	0.10	0.20	0.30	0.35	0.40	0.45
Rotor power (KW)	576	562	646	713	806	932
Root θ , $\psi=90^\circ$ (Deg)	8.70	7.43	6.80	6.55	6.30	5.98
Tip θ , $\psi=90^\circ$ (Deg)	2.75	1.48	0.87	0.71	0.64	0.69
Root θ , $\psi=270^\circ$ (Deg)	11.59	13.12	15.72	17.36	19.26	21.32
Tip θ , $\psi=270^\circ$ (Deg)	5.39	6.97	9.51	11.16	13.00	14.97
Root θ - Tip θ $\psi = 90^\circ$ (Deg)	5.95	5.96	5.92	5.85	5.66	5.29
Root θ - Tip θ $\psi=270^\circ$ (Deg)	6.21	6.15	6.21	6.20	6.25	6.36
P-P flatwise bending mom. (Nm)	525	614	784	695	850	1100
P-P edgewise bending mom. (Nm)	614	899	597	580	987	1218
P-P torsional moment (Nm)	196	216	307	351	463	669
P-P F_x (N)	1357	1405	1390	1222	2546	3448
P-P F_y (N)	1676	2509	1545	911	727	1091
P-P F_z (N)	2280	1870	950	2060	2390	2970

TABLE 16

PERFORMANCE OF THE PUMA BLADE WITH VARIOUS MODIFICATIONS
at $\mu = 0.35$

Blade		Baseline	No1	No2	No3	No4	No5
Modal frequ. (per rev)	1L	0.24	0.24	0.24	0.24	0.23	0.23
	1F	1.03	1.03	1.03	1.03	1.03	1.03
	2F	2.74	2.73	2.71	2.70	2.84	2.82
	2L	4.78	4.75	4.94	4.87	4.80	4.87
	3F	5.32	4.93	4.79	4.49	4.17	4.05
	1T	5.59	5.81	5.84	5.64	5.73	5.85
	4F	9.03	9.13	9.11	9.07	9.34	9.12
	3L	13.88	13.76	13.86	13.56	13.04	13.05
Rotor power (KW)		713	714	710	712	702	709
Root θ , $\psi=90^\circ$		6.55°	5.93°	6.29°	6.02°	6.39°	6.21°
Tip θ , $\psi=90^\circ$		0.71°	0.87°	1.03°	0.99°	0.88°	1.14°
Root θ , $\psi=270^\circ$		17.36°	17.20°	17.13°	17.07°	18.93°	18.44°
Tip θ , $\psi=270^\circ$		11.16°	11.11°	11.16°	10.86°	11.11°	10.66°
Root θ - Tip θ $\psi = 90^\circ$ (Deg)		5.85	5.06	5.26	5.03	5.50	5.07
Root θ - Tip θ $\psi=270^\circ$ (Deg)		6.20	6.08	5.97	6.21	7.83	7.77
P-P flatwise bending mom. (Nm)		695	607	732	735	581	542
P-P edgewise bending mom. (Nm)		580	593	679	621	592	647
P-P torsional moment (Nm)		351	521	423	448	573	715
P-P Fx (N)		1222	1219	1465	1496	1392	1713
P-P Fy (N)		911	1231	1093	1075	1229	1259
P-P Fz (N)		2060	2290	2600	2730	2800	3210
Tip Mn, $\psi=90^\circ$		0.829	0.799	0.798	0.798	0.799	0.801

TABLE 17

**PERFORMANCE OF THE PUMA BLADE WITH SWEPT TIP AND
CAMBERED AEROFOIL SECTION**

Advance ratio	0.10	0.20	0.30	0.35	0.40	0.45
Rotor power (KW)	589	583	663	727	813	950
Root θ, $\psi=90^\circ$ (Deg)	8.17	6.90	6.45	6.31	6.05	5.90
Tip θ, $\psi=90^\circ$ (Deg)	0.75	-0.52	-1.05	-1.21	-1.50	-1.42
Root θ, $\psi=270^\circ$ (Deg)	10.82	12.17	14.66	16.17	17.76	19.43
Tip θ, $\psi=270^\circ$ (Deg)	2.87	4.55	7.08	8.31	9.57	10.71
Root θ- Tip θ $\psi = 90^\circ$ (Deg)	7.42	7.42	7.50	7.53	7.55	7.32
Root θ- Tip θ $\psi=270^\circ$ (Deg)	7.96	7.62	7.58	7.86	8.19	8.72
P-P flatwise bending mom. (Nm)	334	376	558	674	826	1076
P-P edgewise bending mom. (Nm)	488	949	848	961	1614	2224
P-P torsional moment (Nm)	266	216	329	377	558	993
P-P Fx (N)	1028	1229	1436	1545	2786	3607
P-P Fy (N)	1386	2358	684	1375	2935	5220
P-P Fz (N)	1910	1170	1380	2230	2470	4770

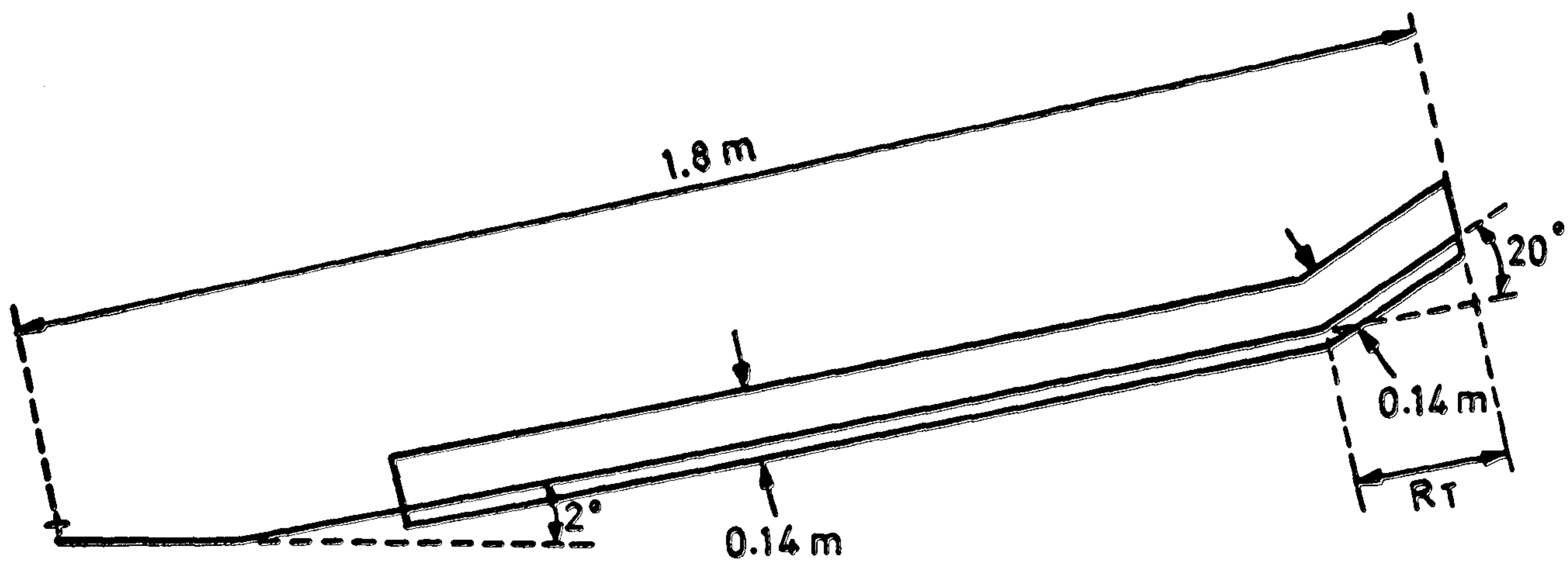
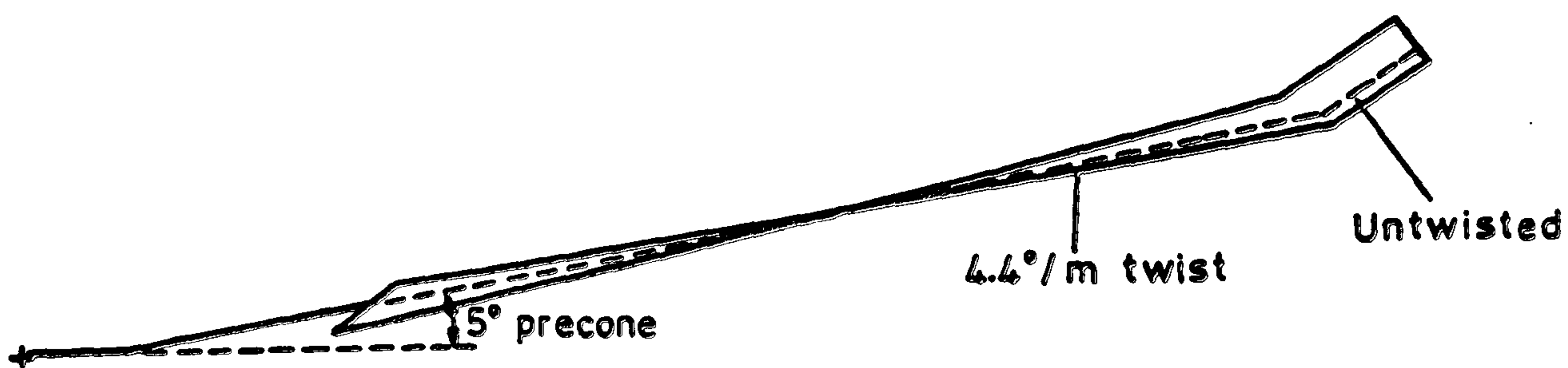


Fig 1 Geometry of the swept tip blade

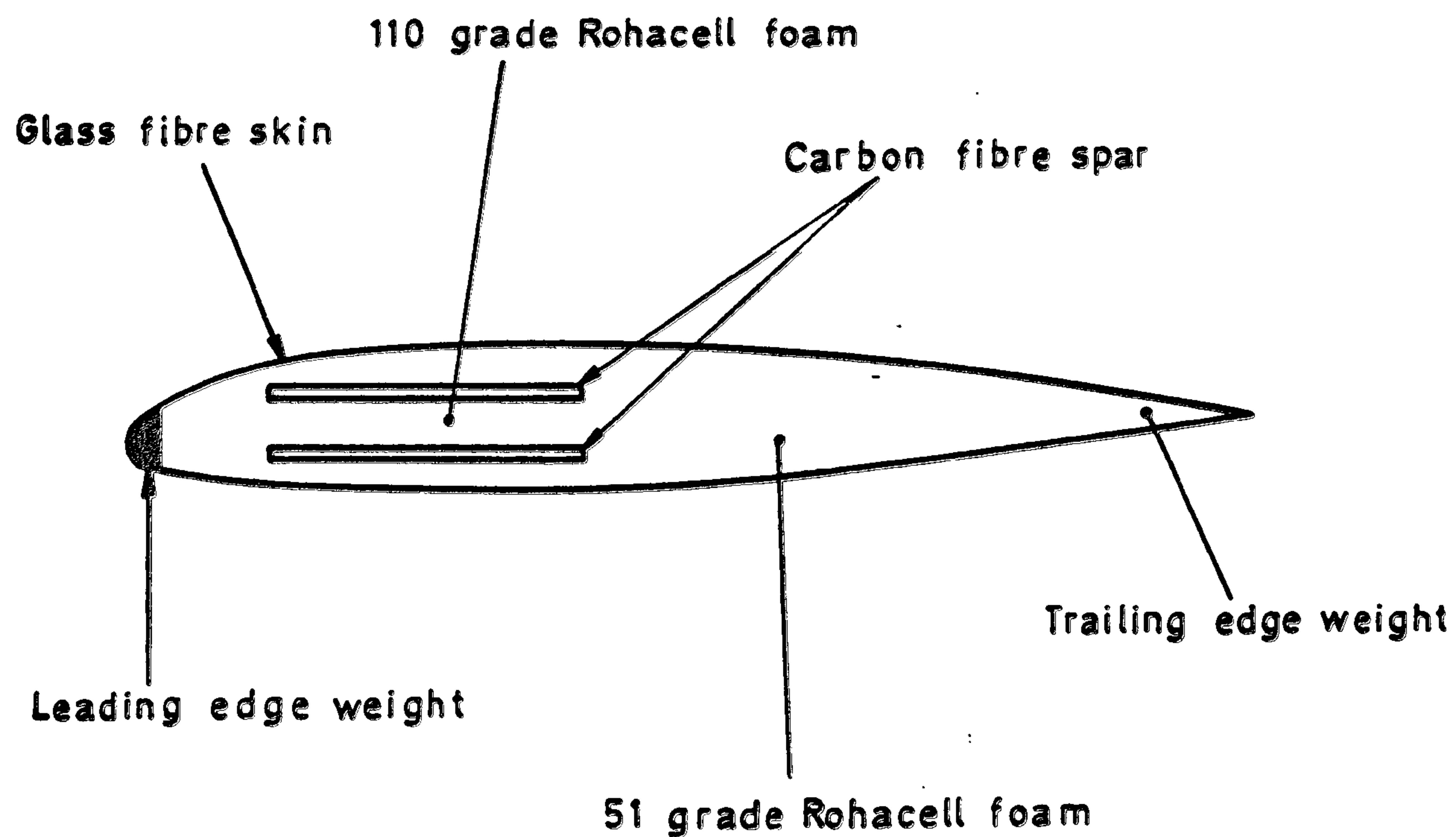
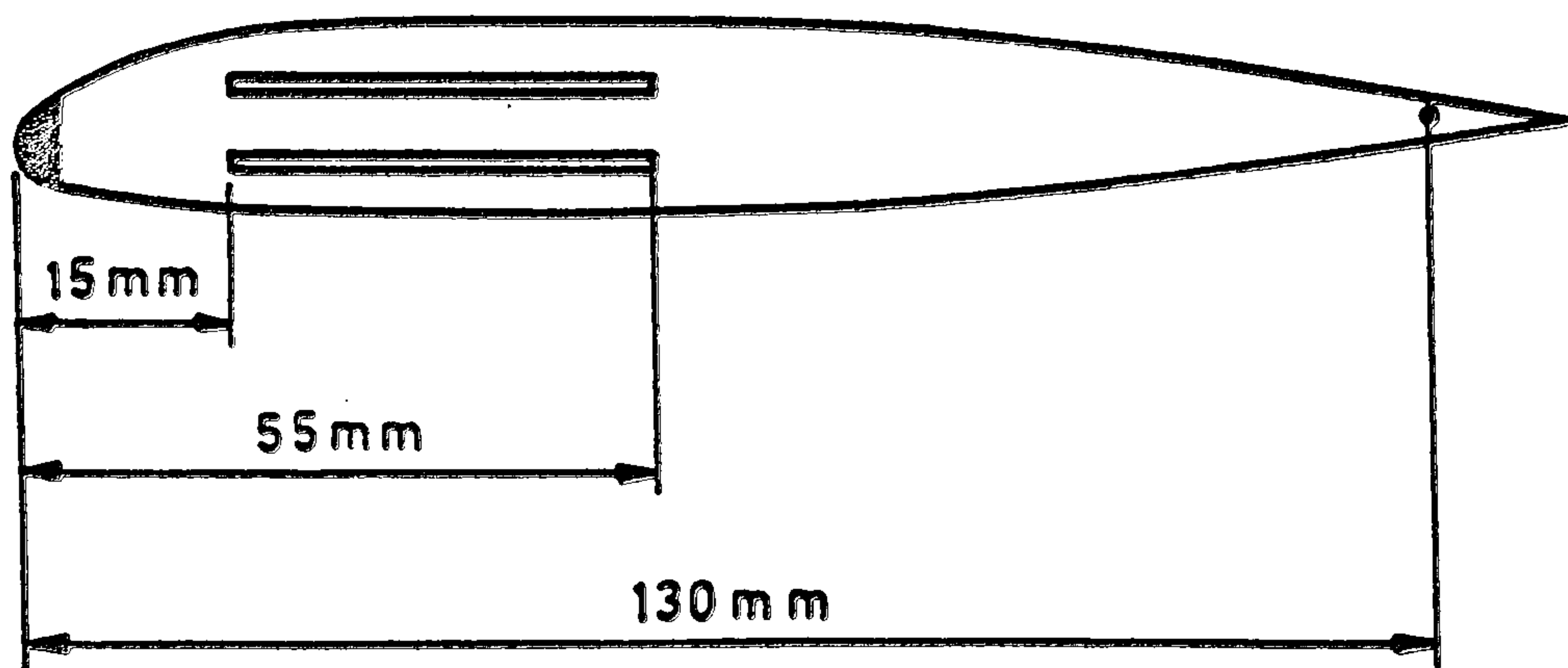


Fig 2 Construction of the baseline rectangular blade

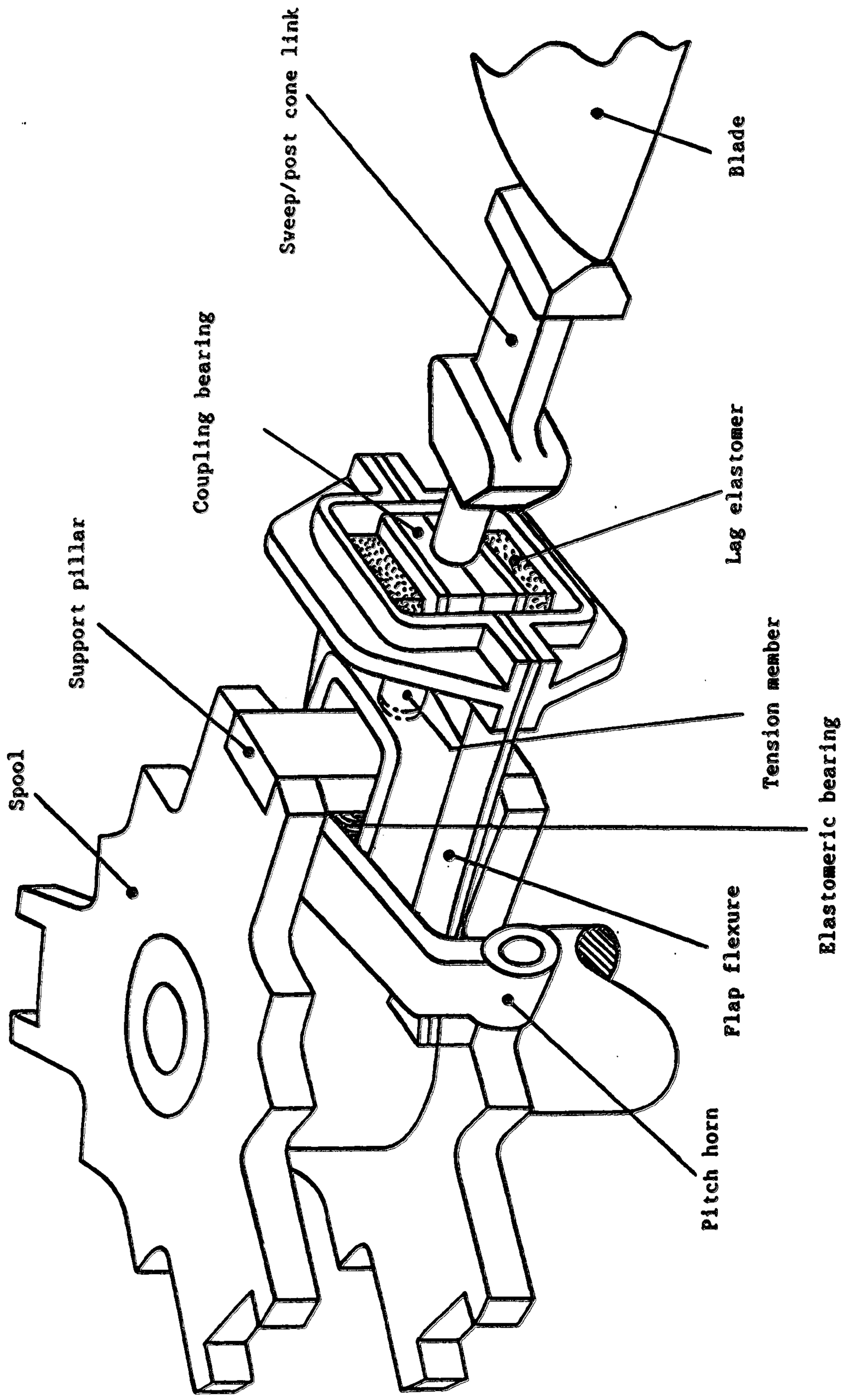


Fig 3 Dual load path rotor system

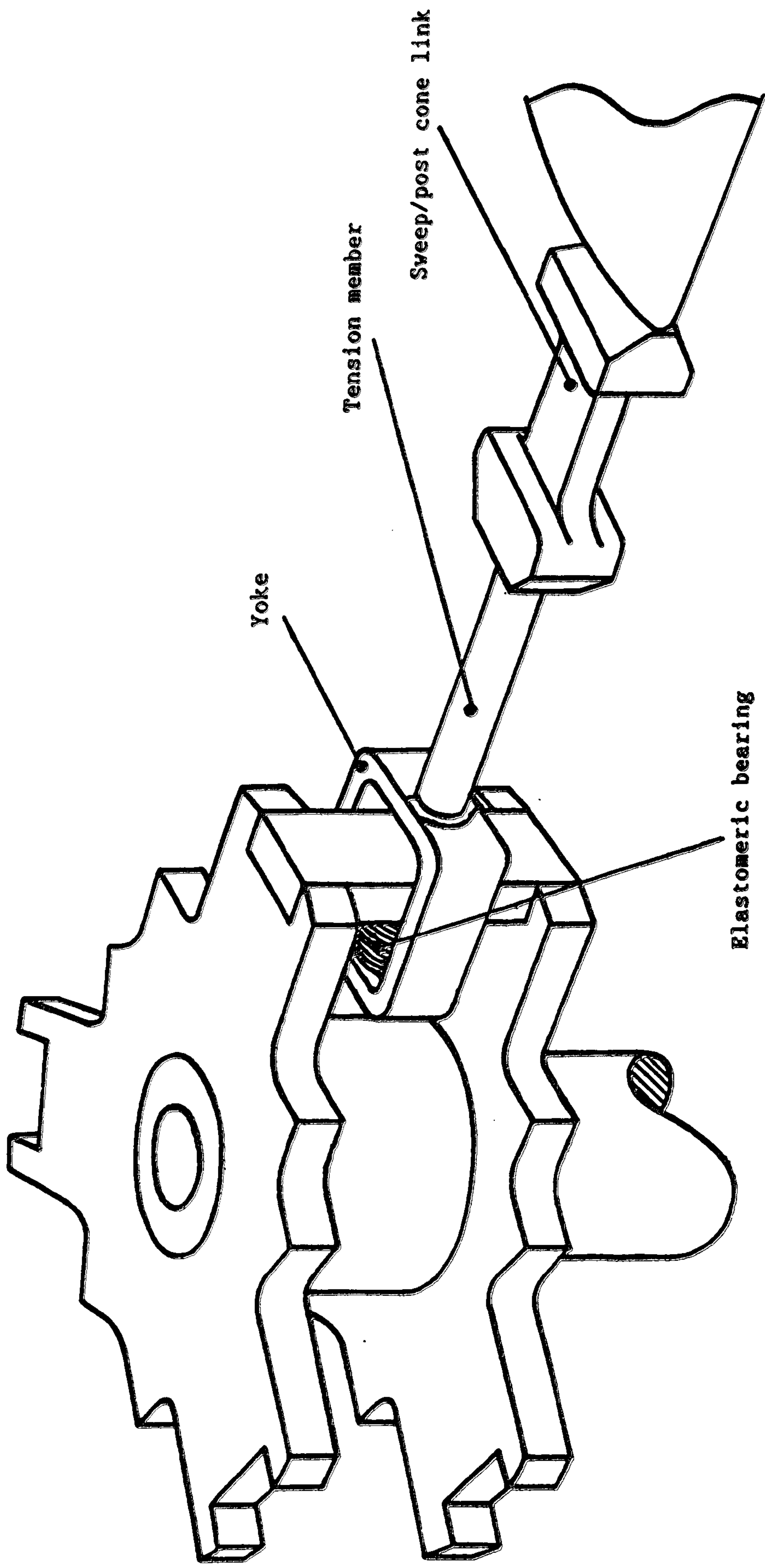


Fig 4 Dual load path rotor system (cf carrying elements)

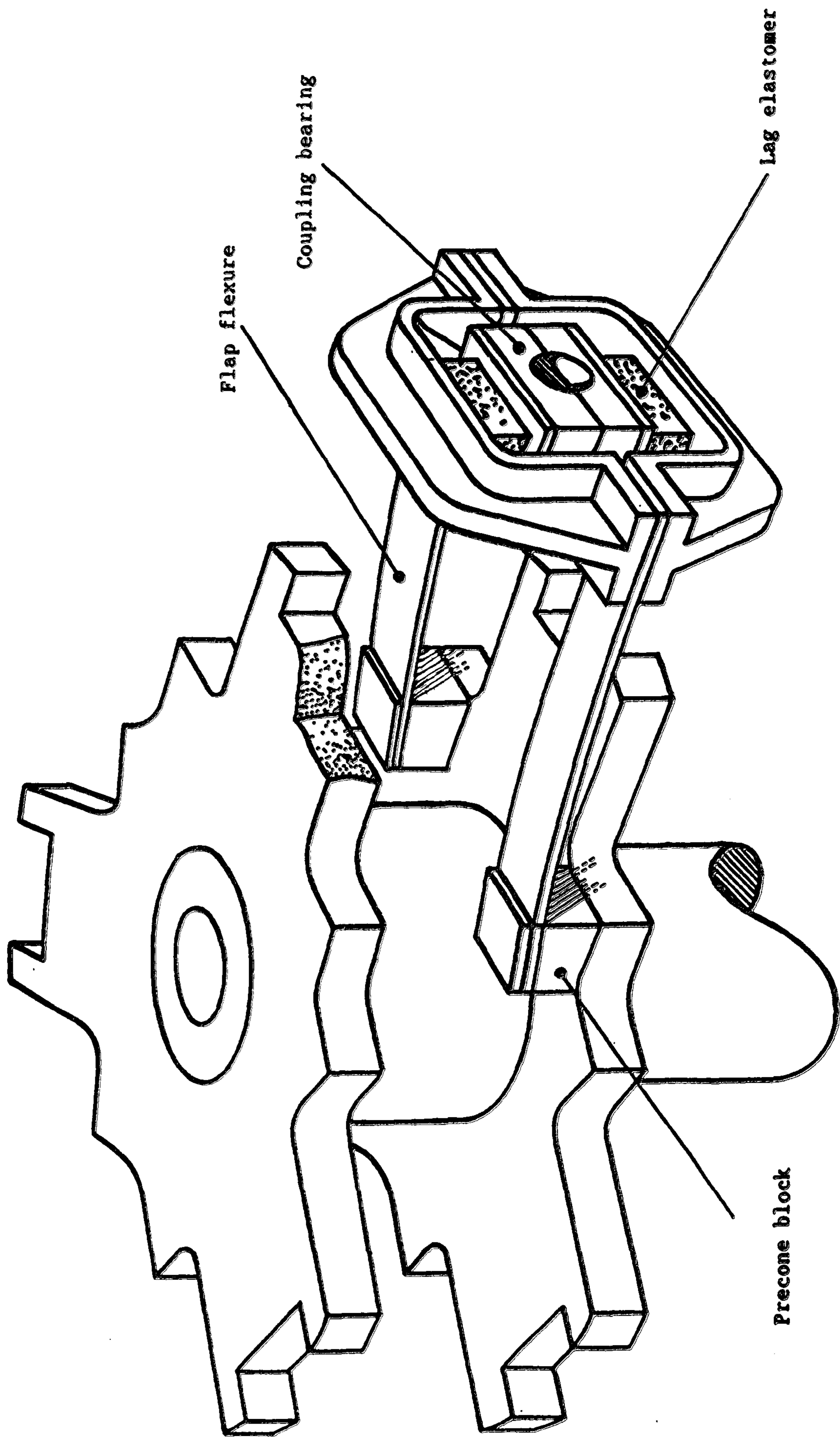


Fig 5 Dual load path rotor system (flap and lag flexure elements)

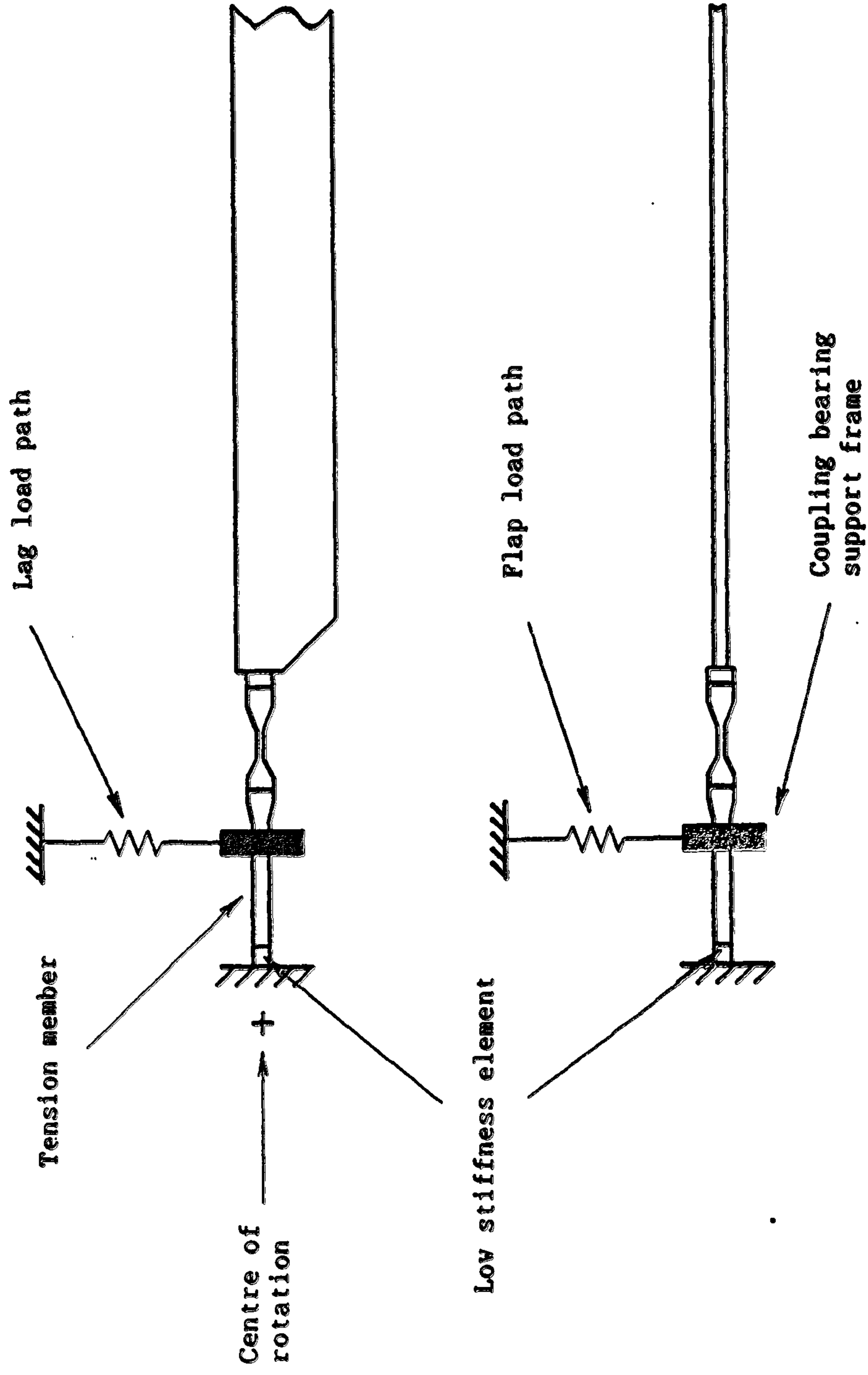


Fig 6 Hub model in the J134 modes program

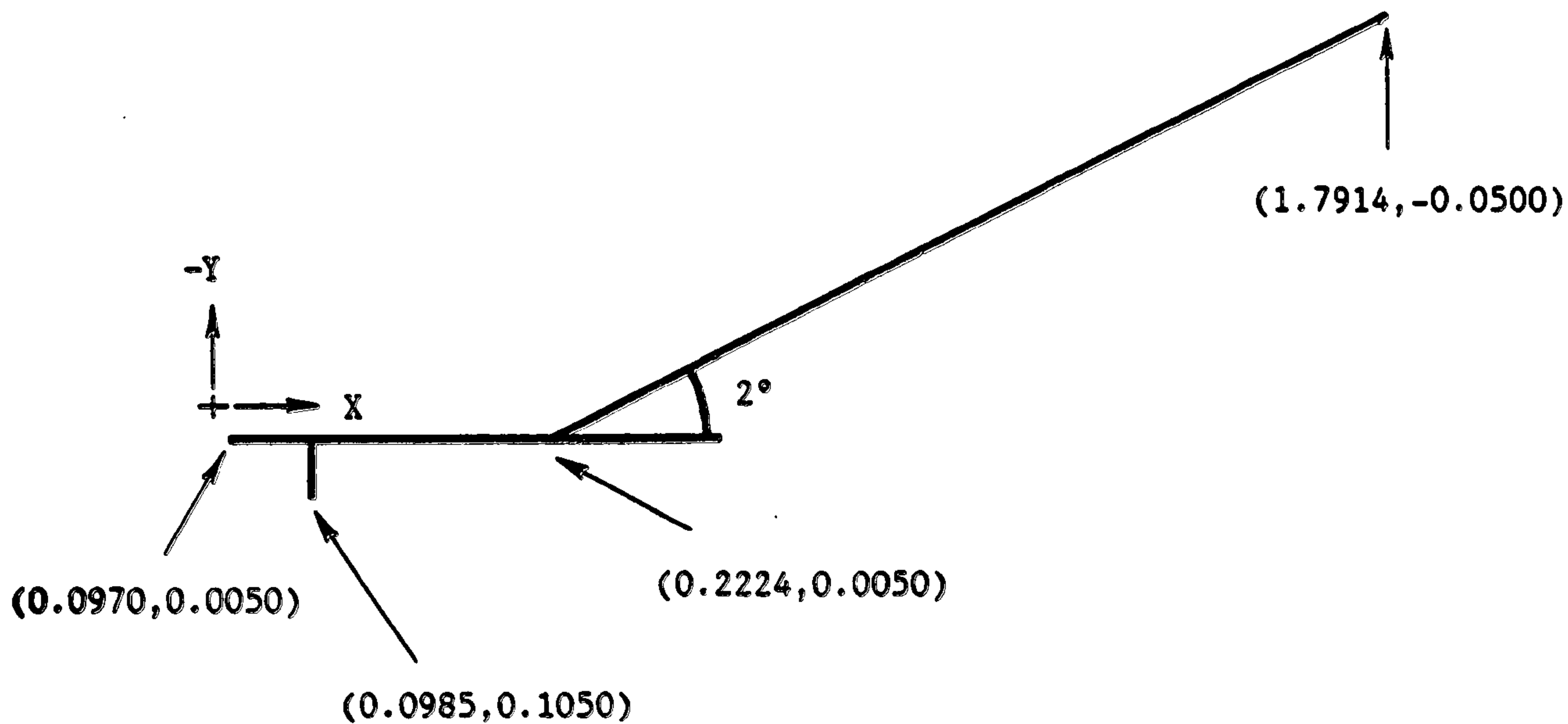
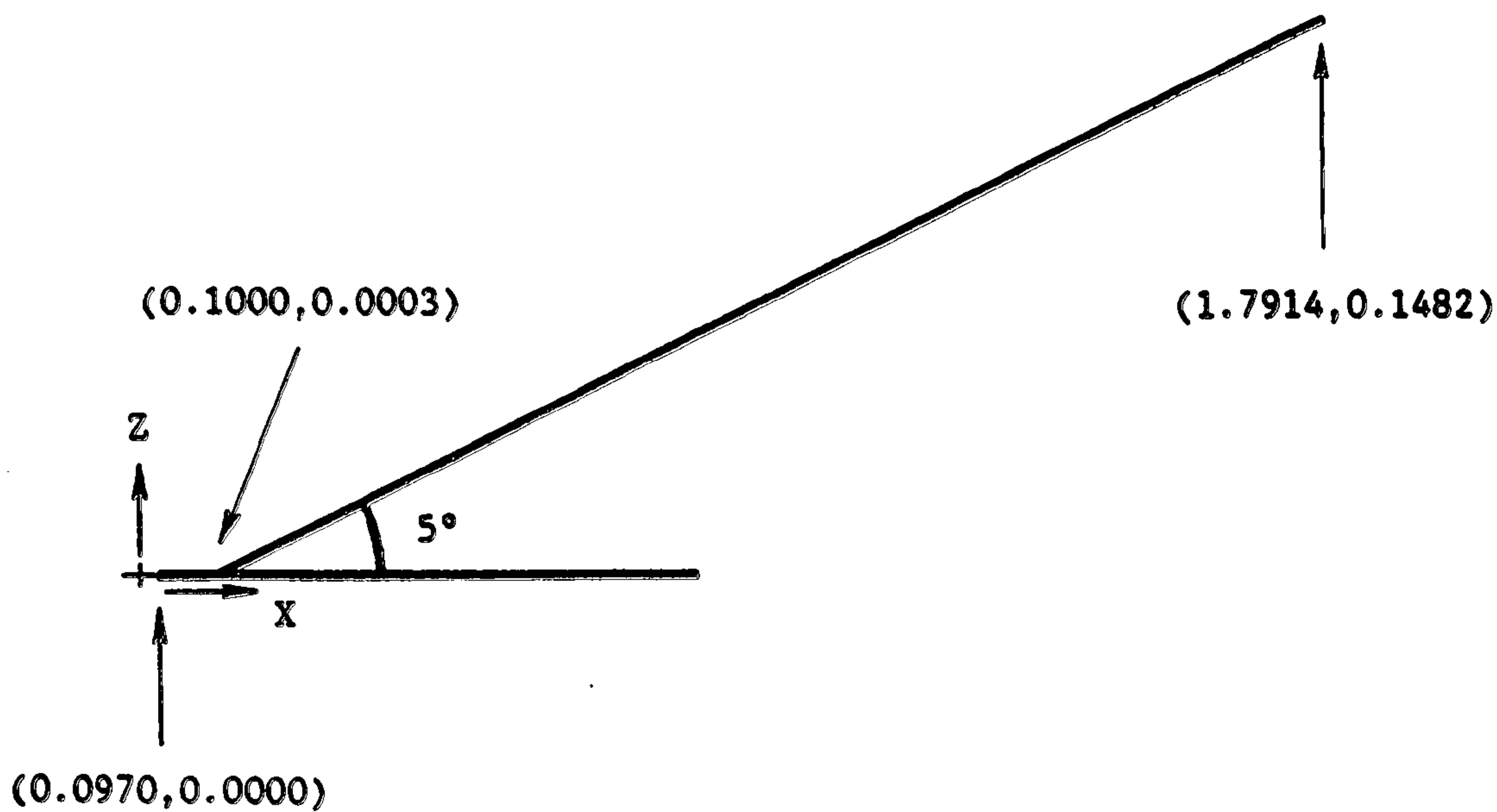
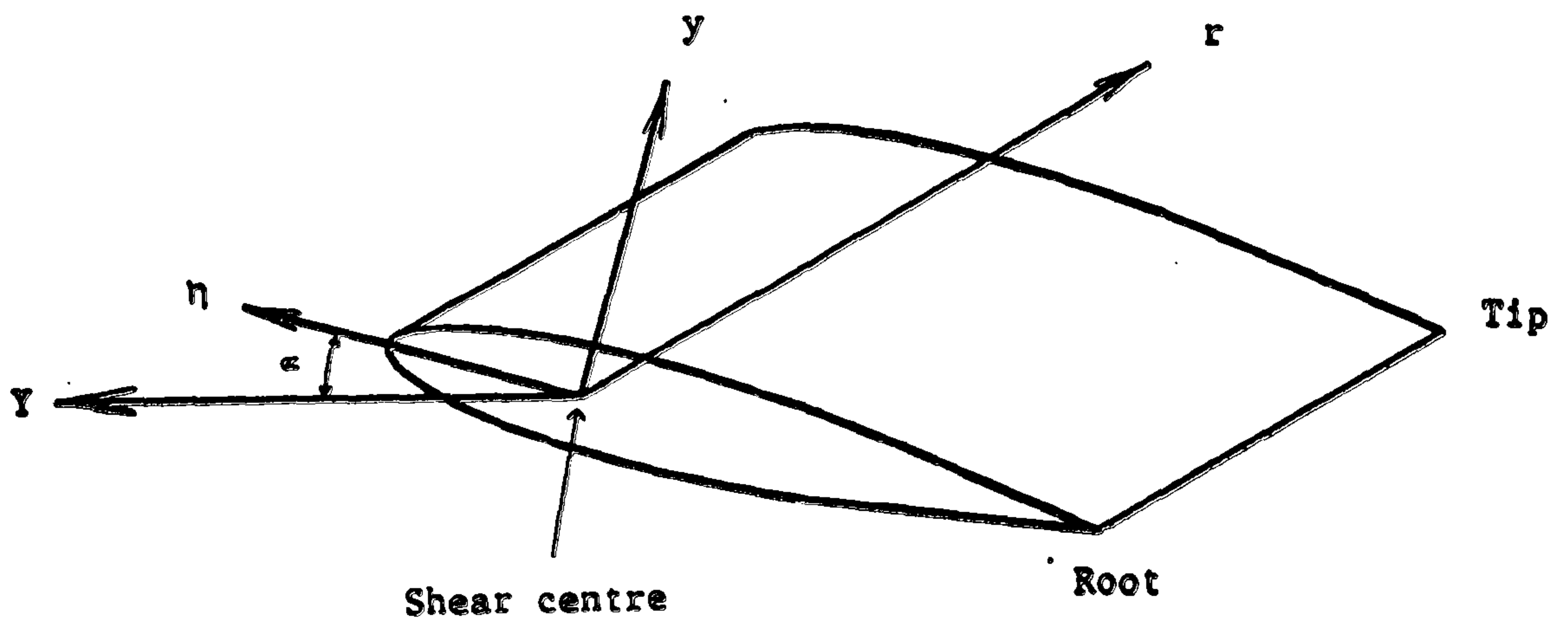
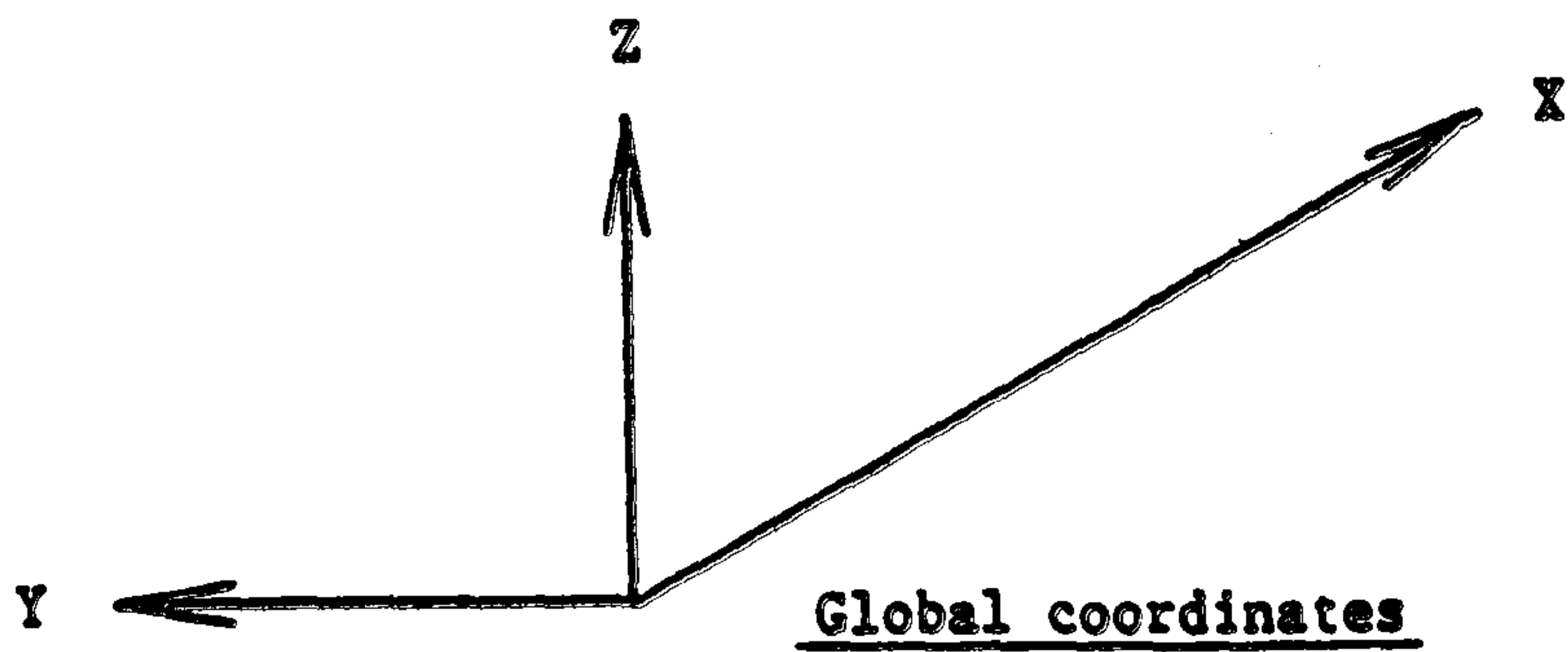


Fig 7 Baseline blade global coordinates

Local coordinates



Global coordinates

Fig 8 Local and global coordinate system

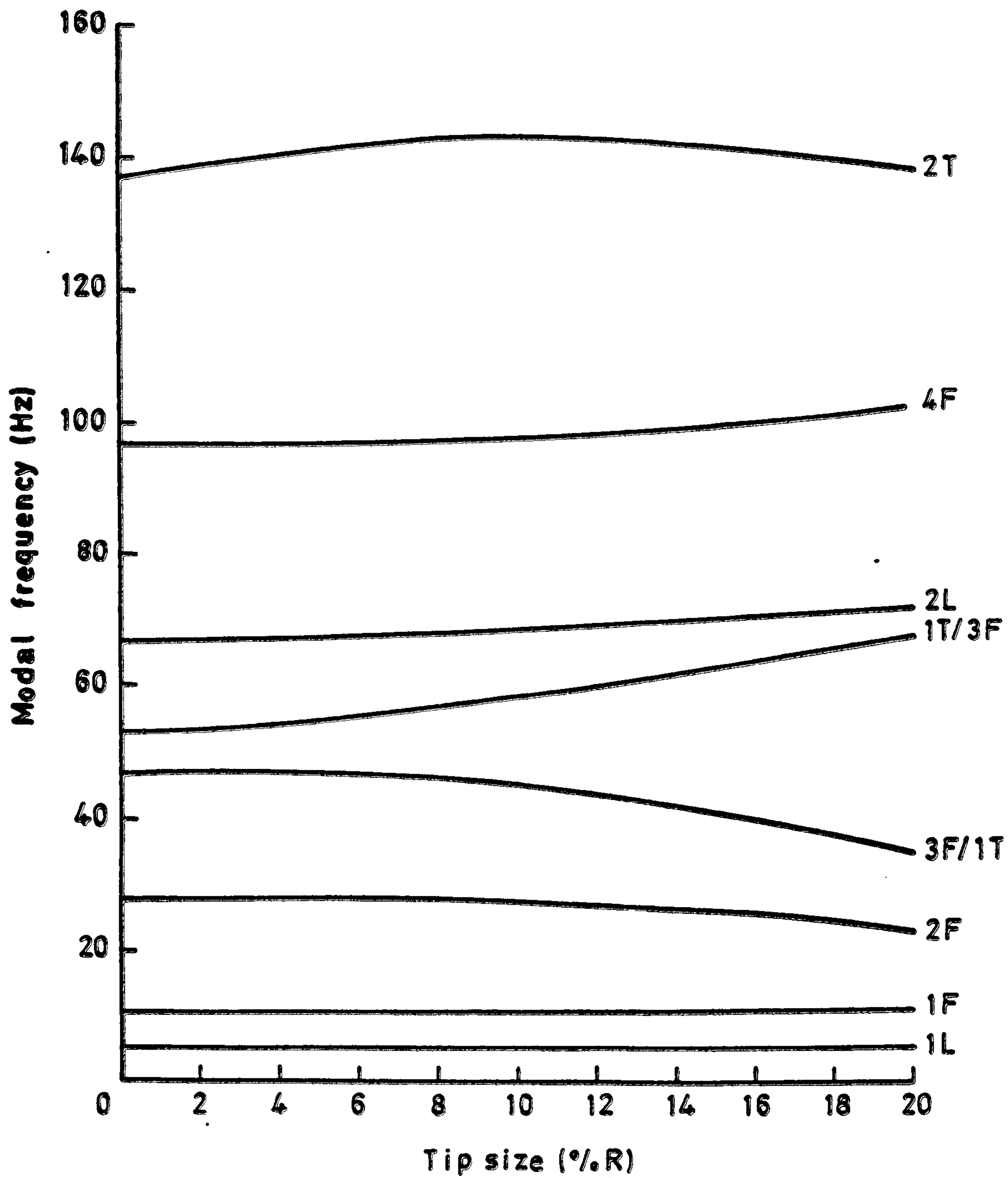


Fig 9 Variation of modal frequencies with tip size (600 rev/min)

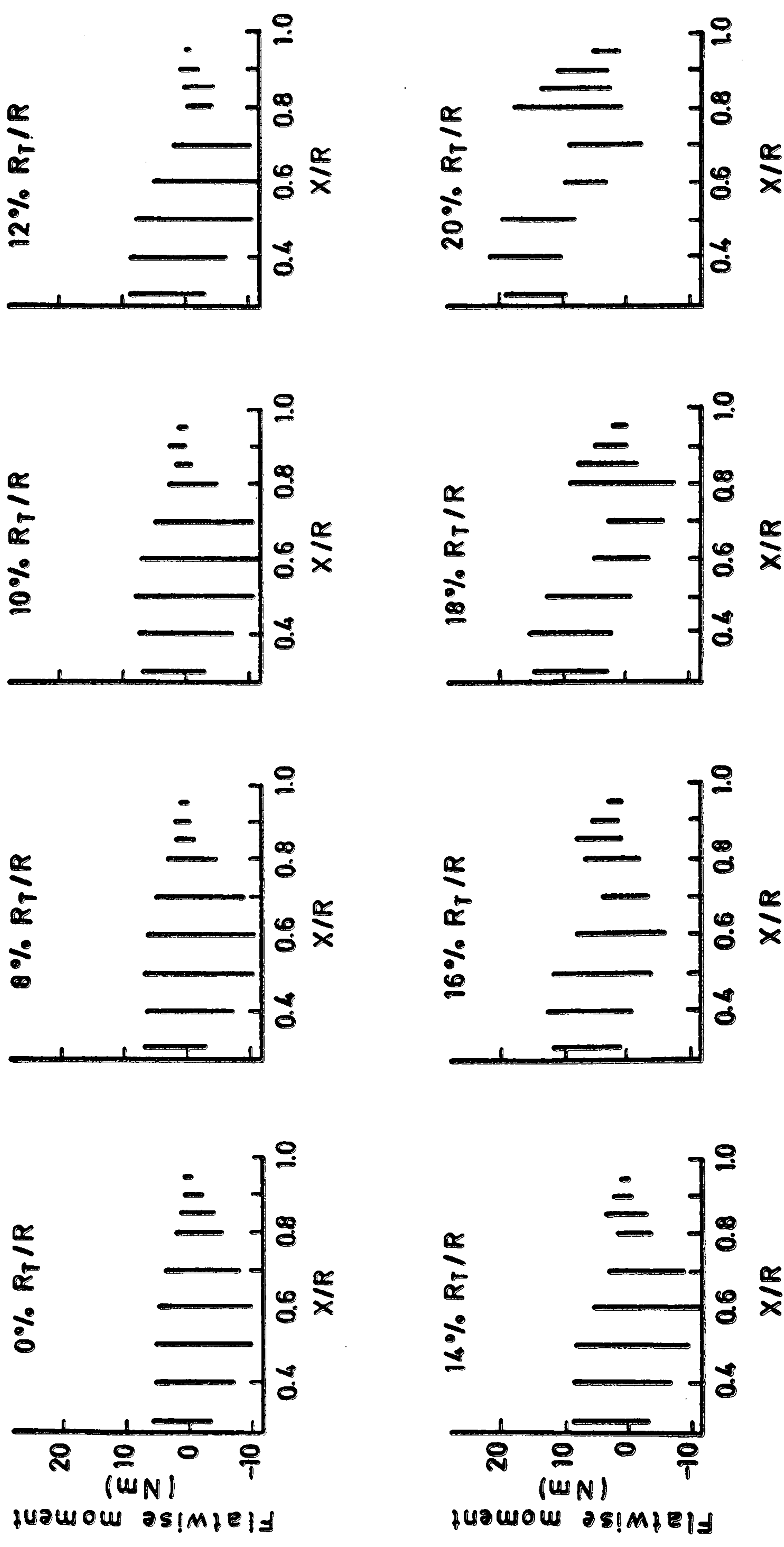


Fig 10 Spanwise variation of flatwise bending moment for various tip sizes

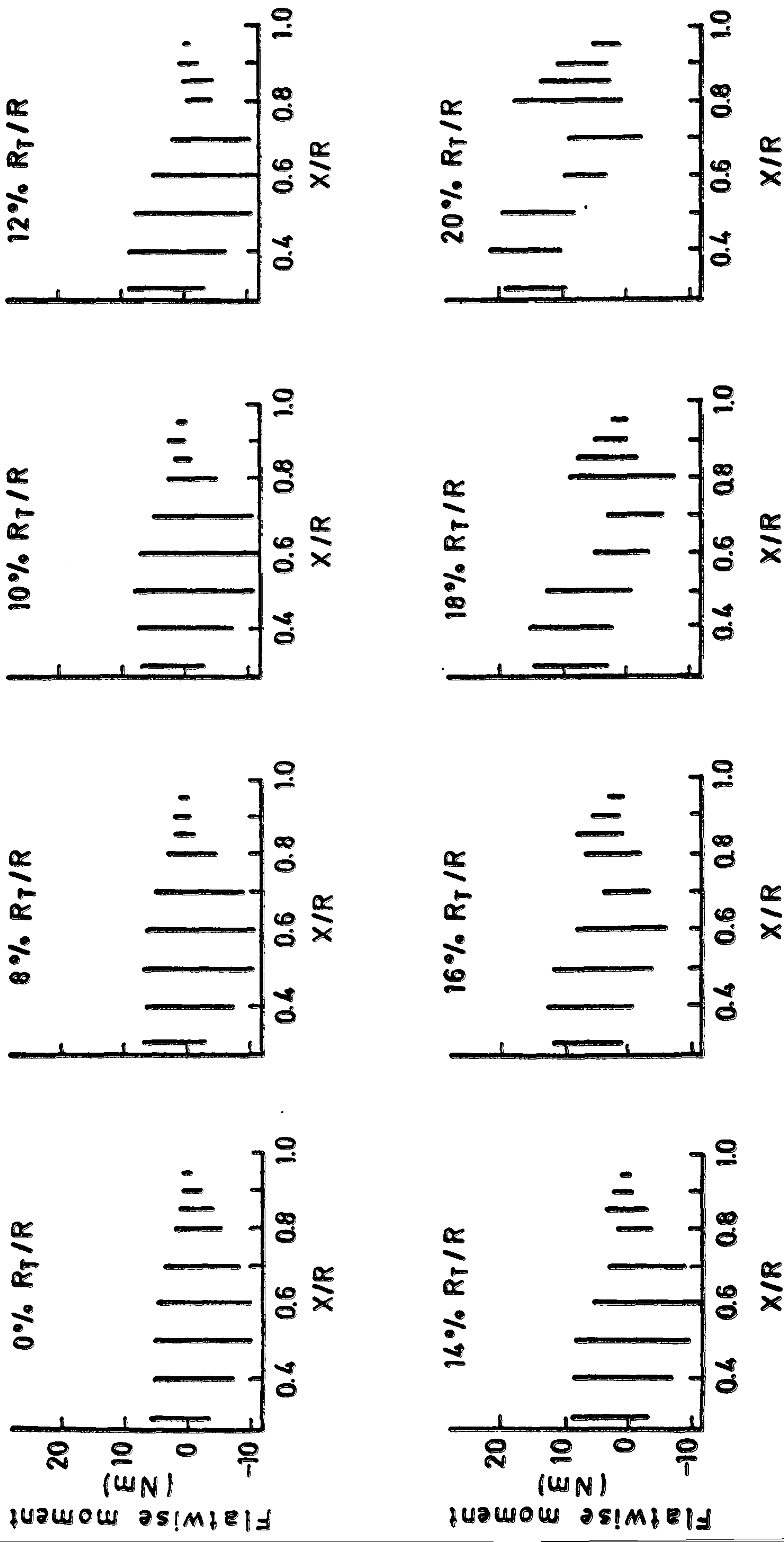


Fig 10 Spanwise variation of flatwise bending moment for various tip sizes

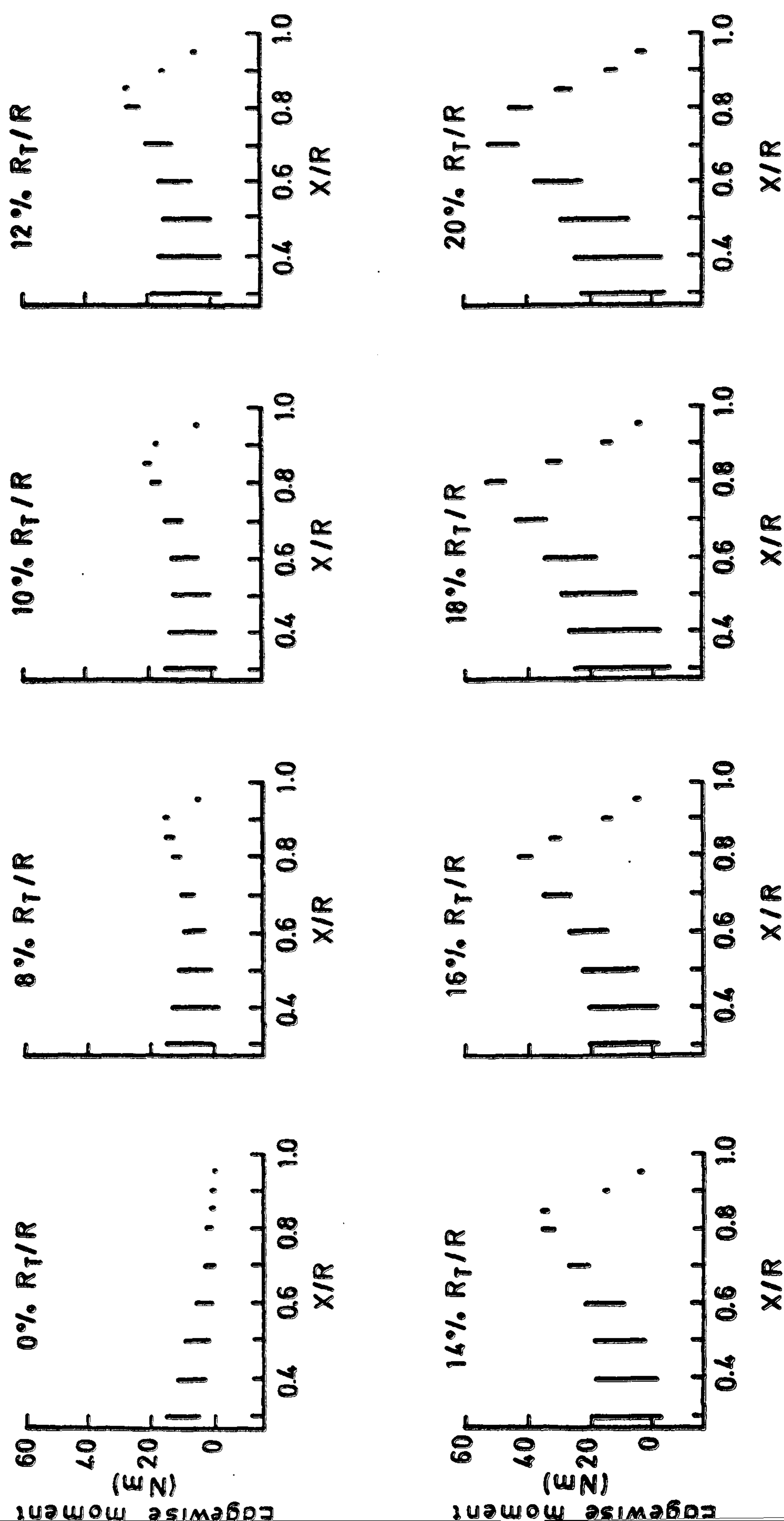


Fig 11 Spanwise variation of edgewise bending moment for various tip sizes

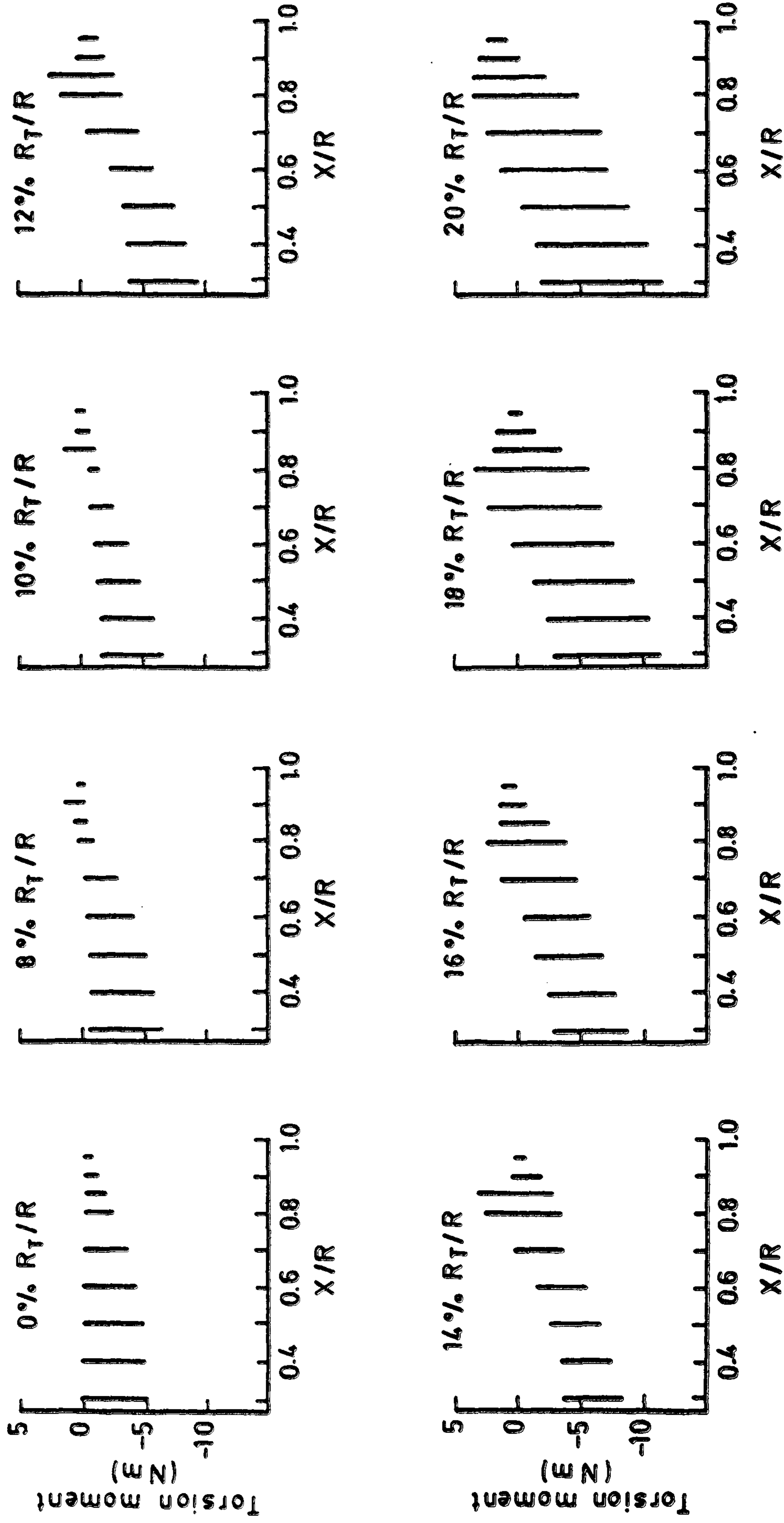


Fig 12 Spanwise variation of torsional moment for various tip sizes

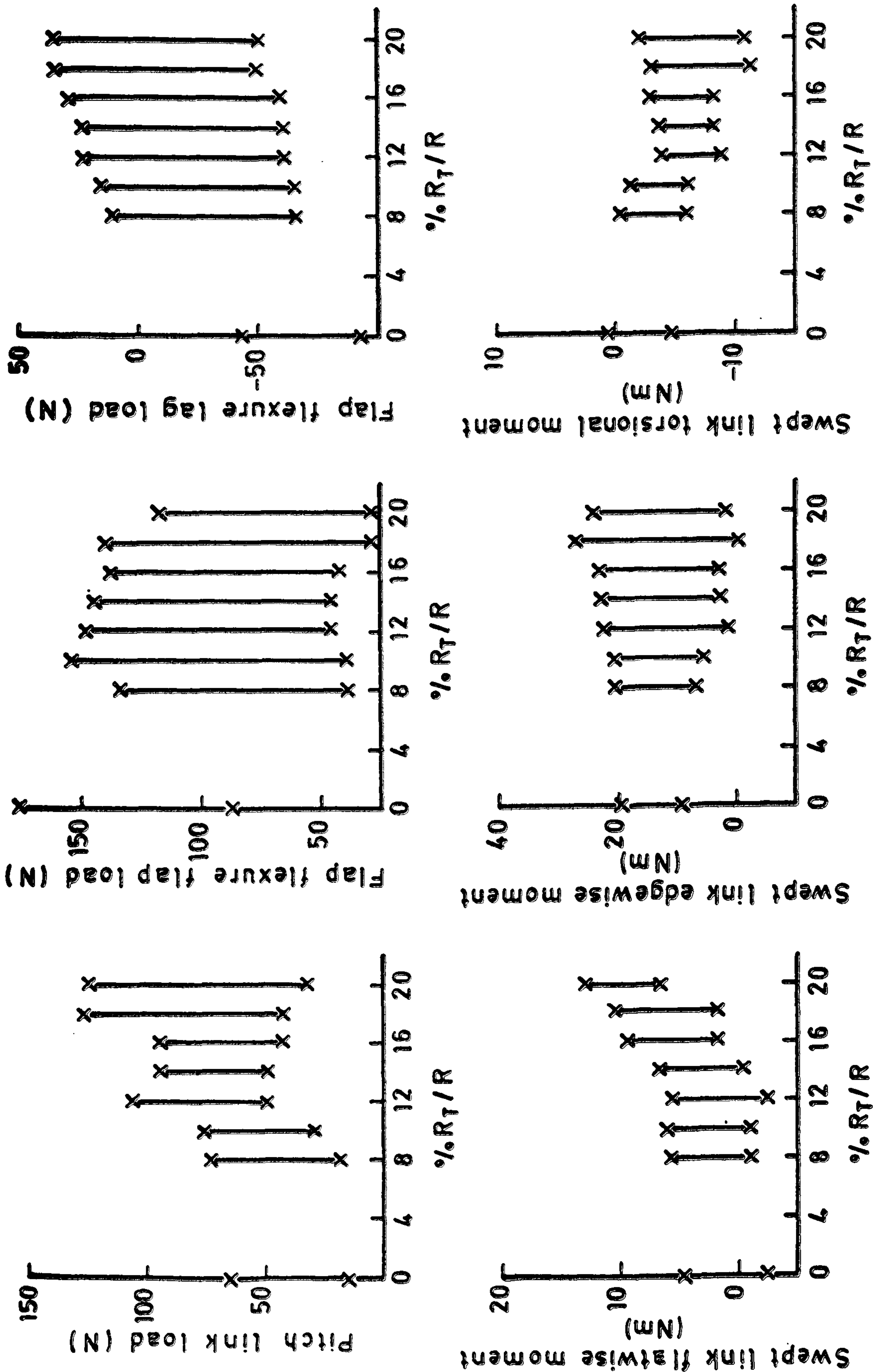


Fig 13 Maximum and minimum hub loads vs tip size

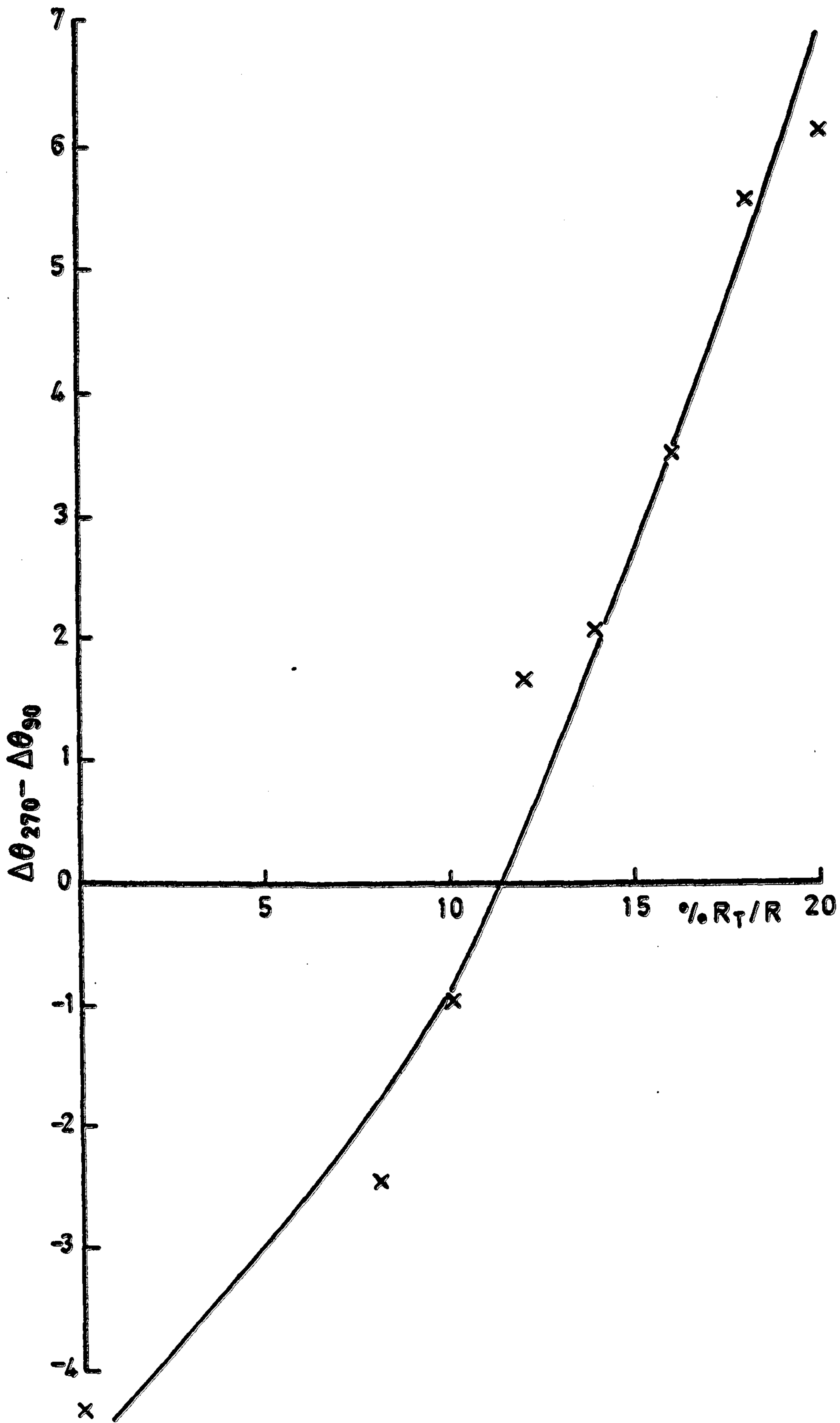


Fig 14 Difference between retreating and advancing blade twist vs tip size

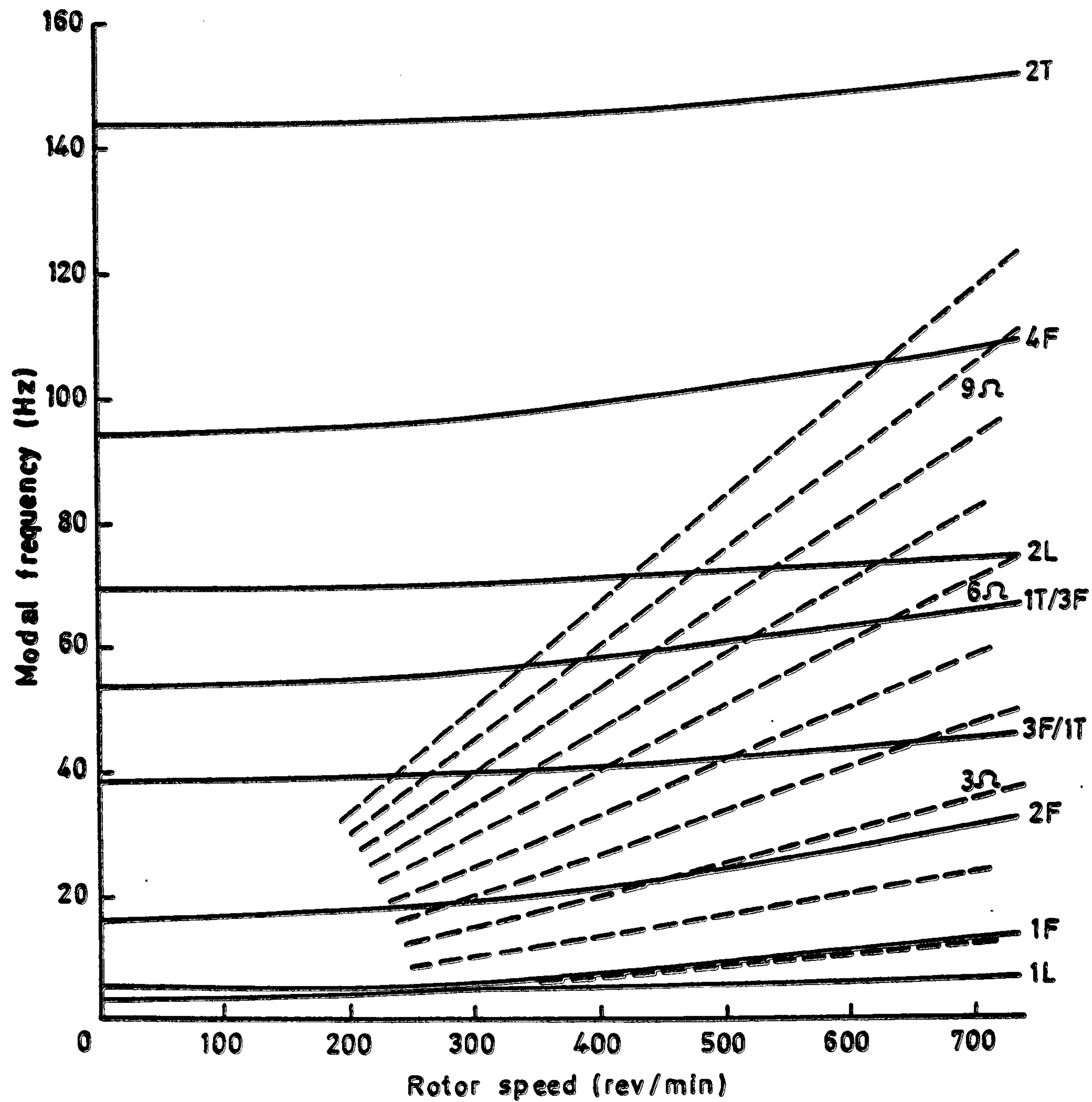


Fig 15 Spoke diagram of the undamped modal frequencies for the Mk1 swept tip blade

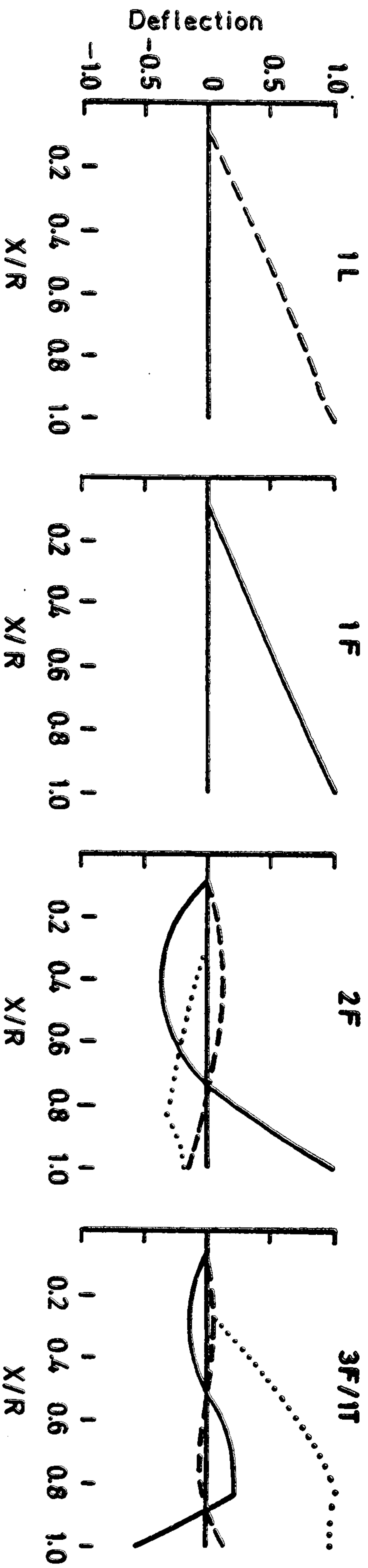
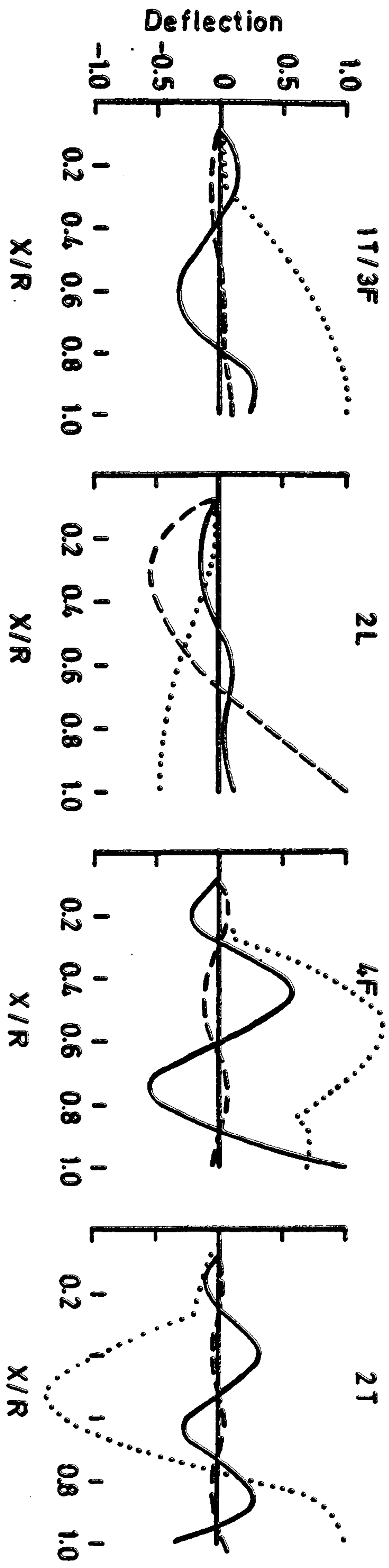


Fig 16 Mode shapes for the Mk1 swept tip blade (600 rev/min)

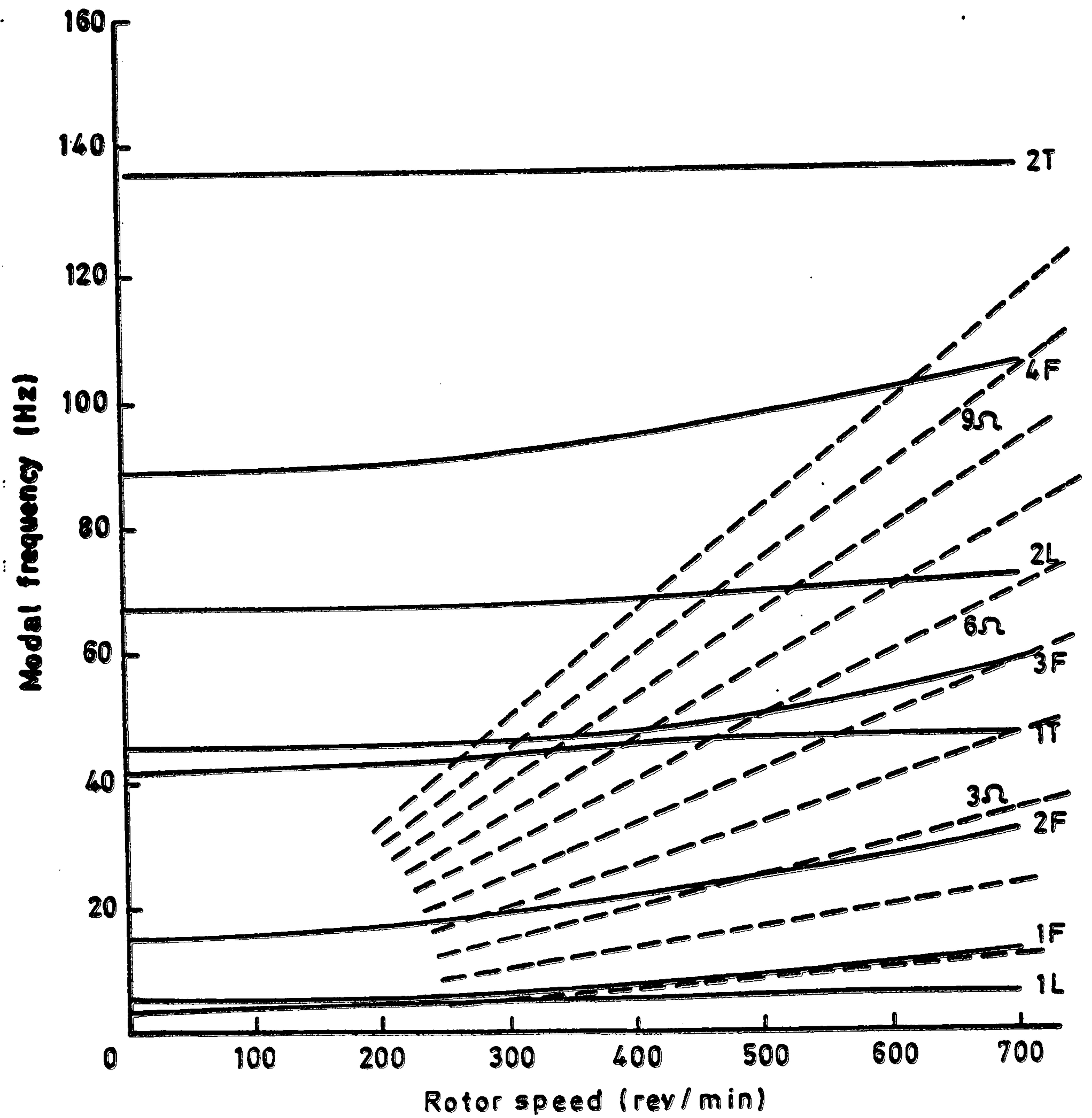


Fig 17 Spoke diagram of the undamped modal frequencies for the baseline rectangular blade

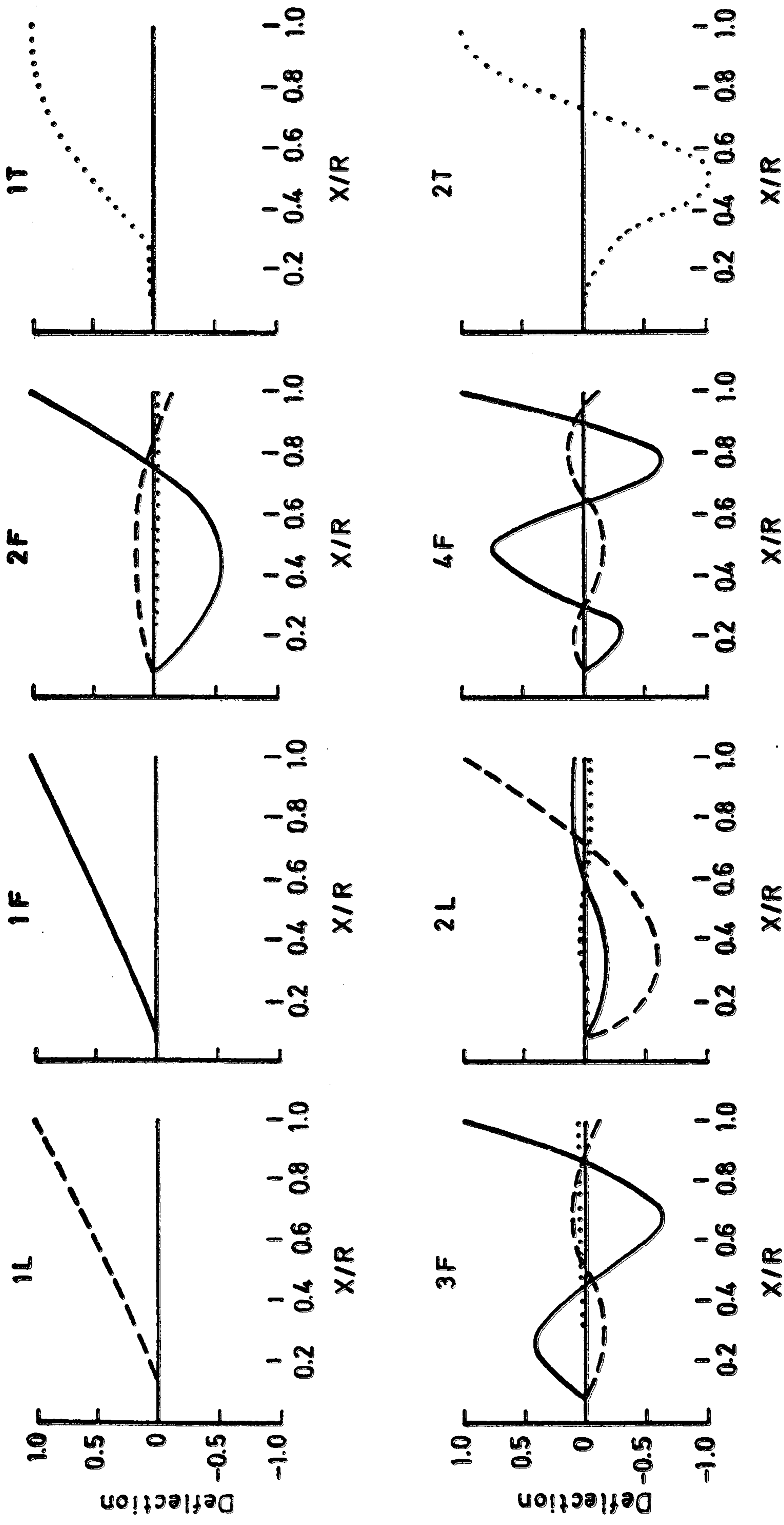


Fig 18 Mode shapes for the baseline rectangular blade (600 rev/min)

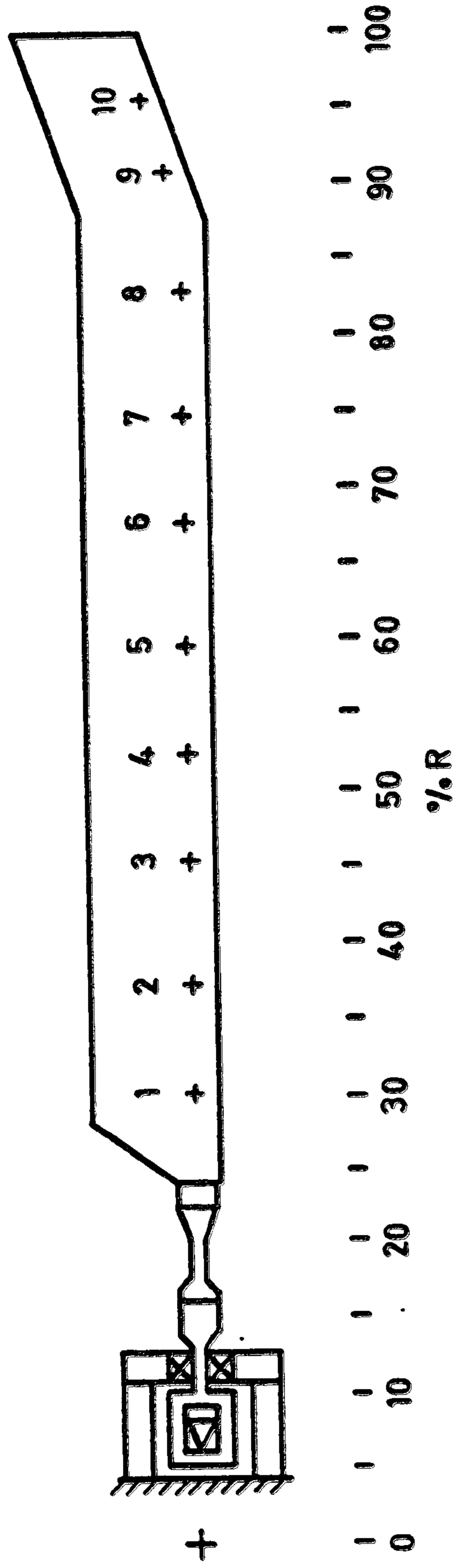


Fig 19 Spanwise location of strain gauges on the swept tip blade

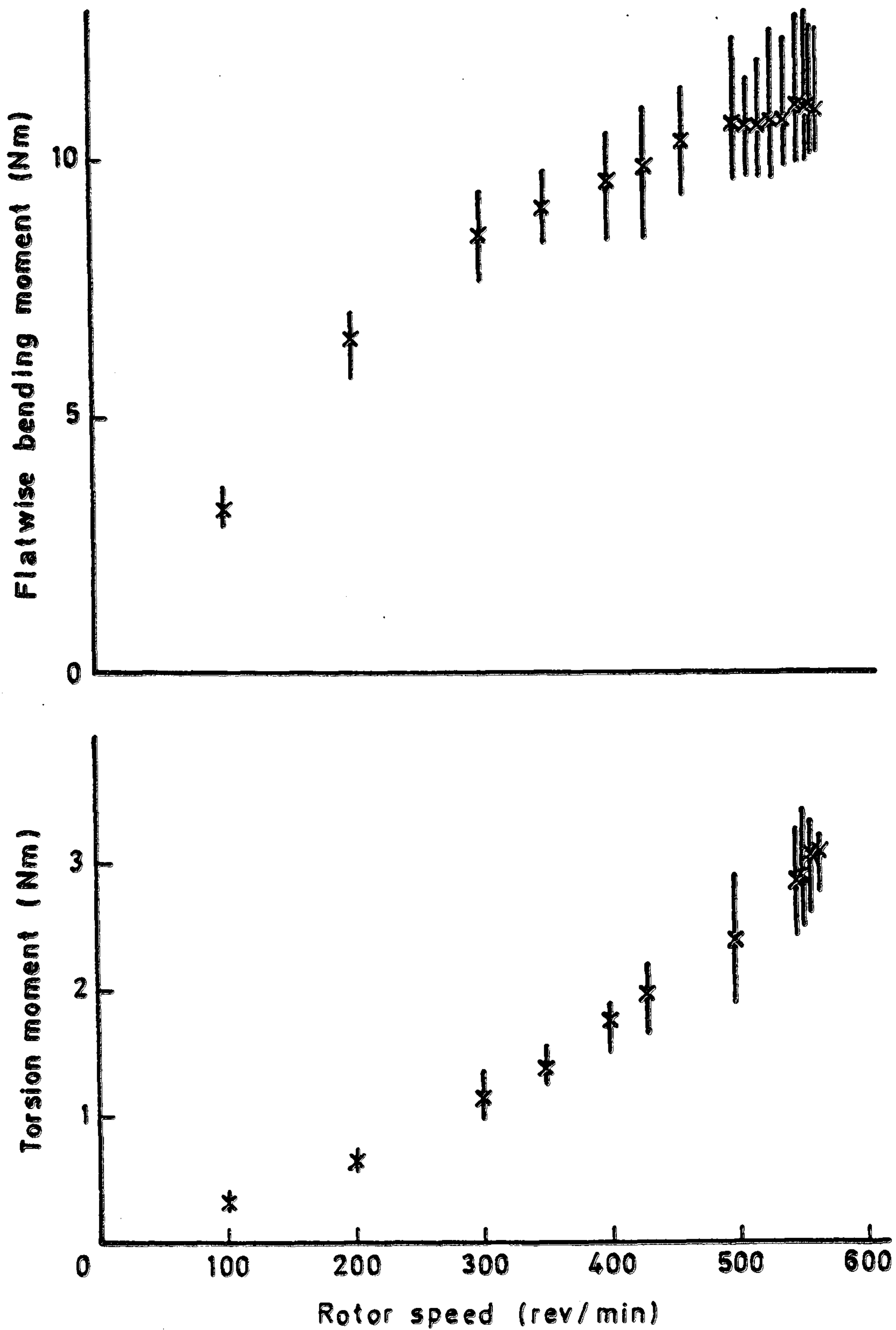


Fig 20 Maximum, mean and minimum blade root flatwise bending and torsional moments

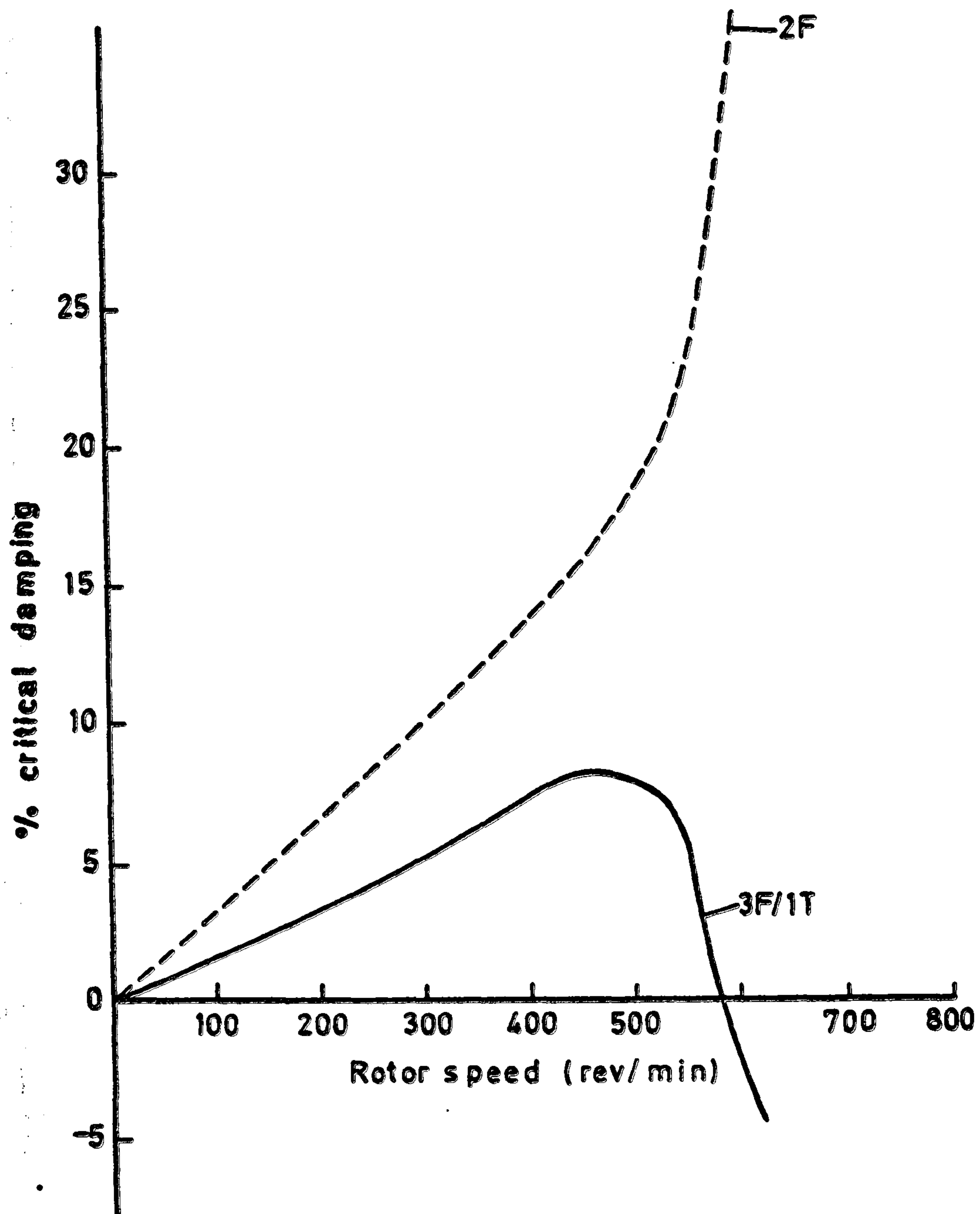


Fig 21 2F and 3F/1T modal damping vs rotor speed for the Mk 1 swept tip blade

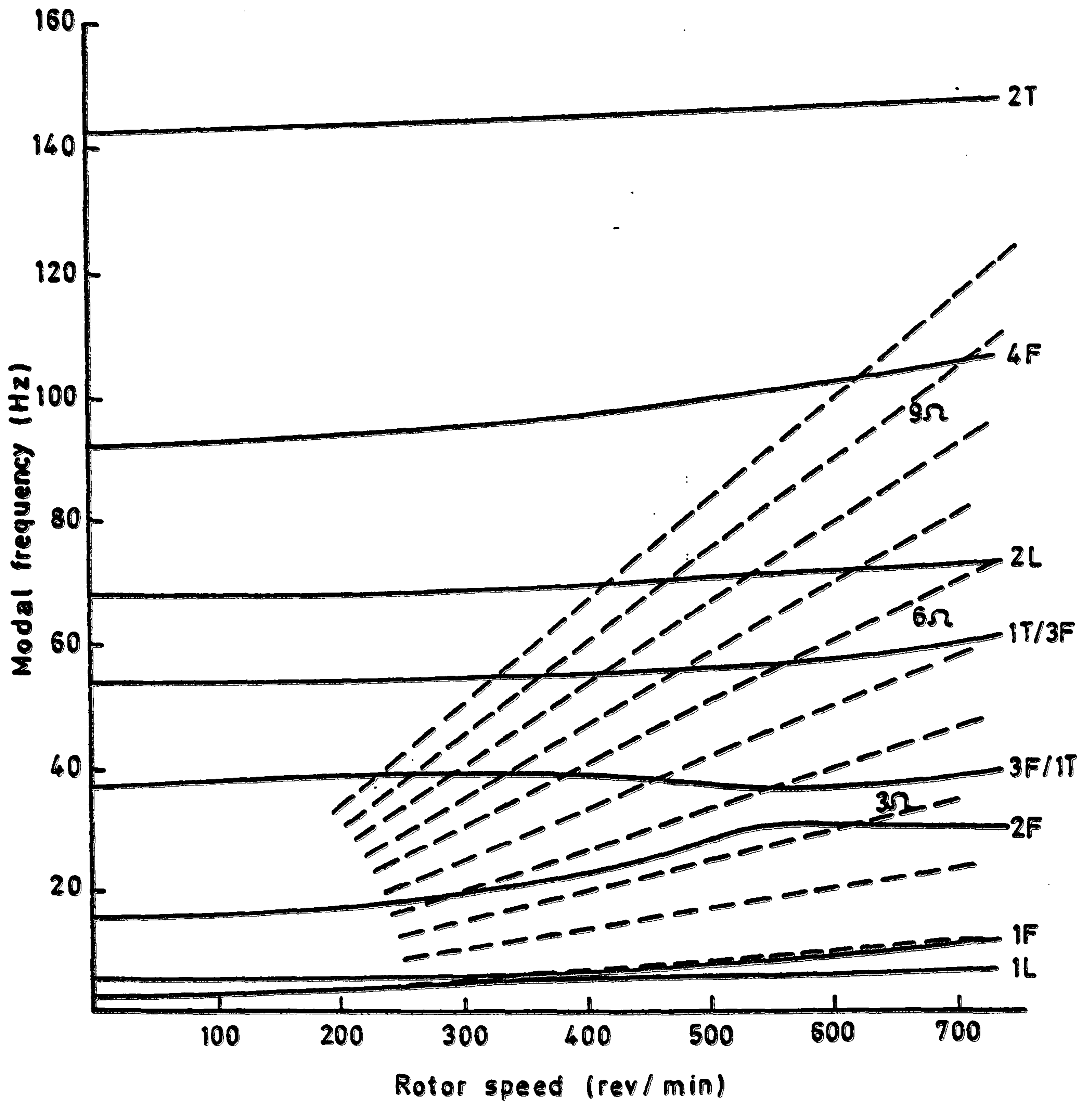


Fig 22 Spoke diagram of the damped modal frequencies for the Mk1 swept tip blade

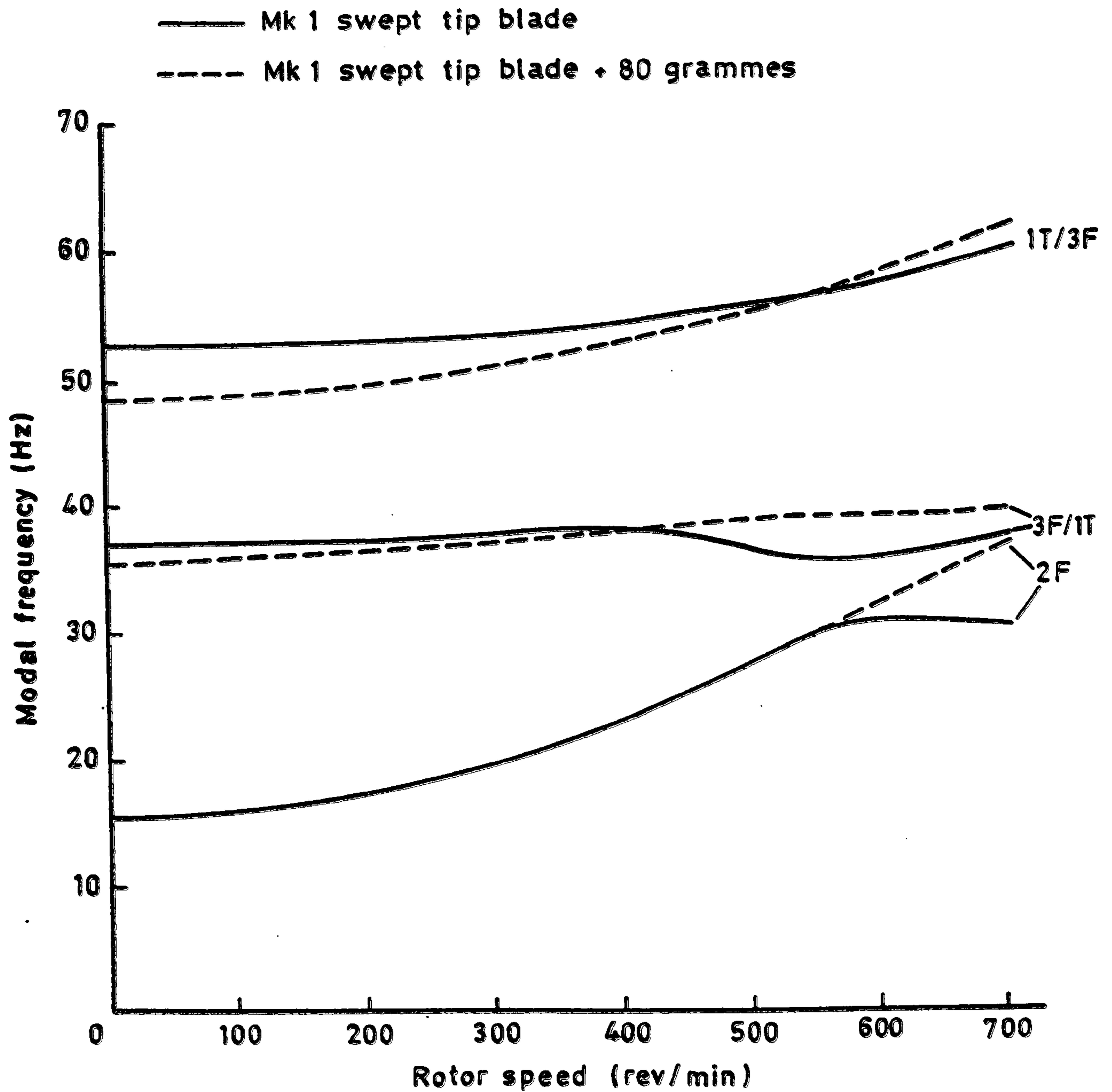


Fig 23 Plot of modal frequency vs rotor speed showing the effect of adding an 80 gramme concentrated mass

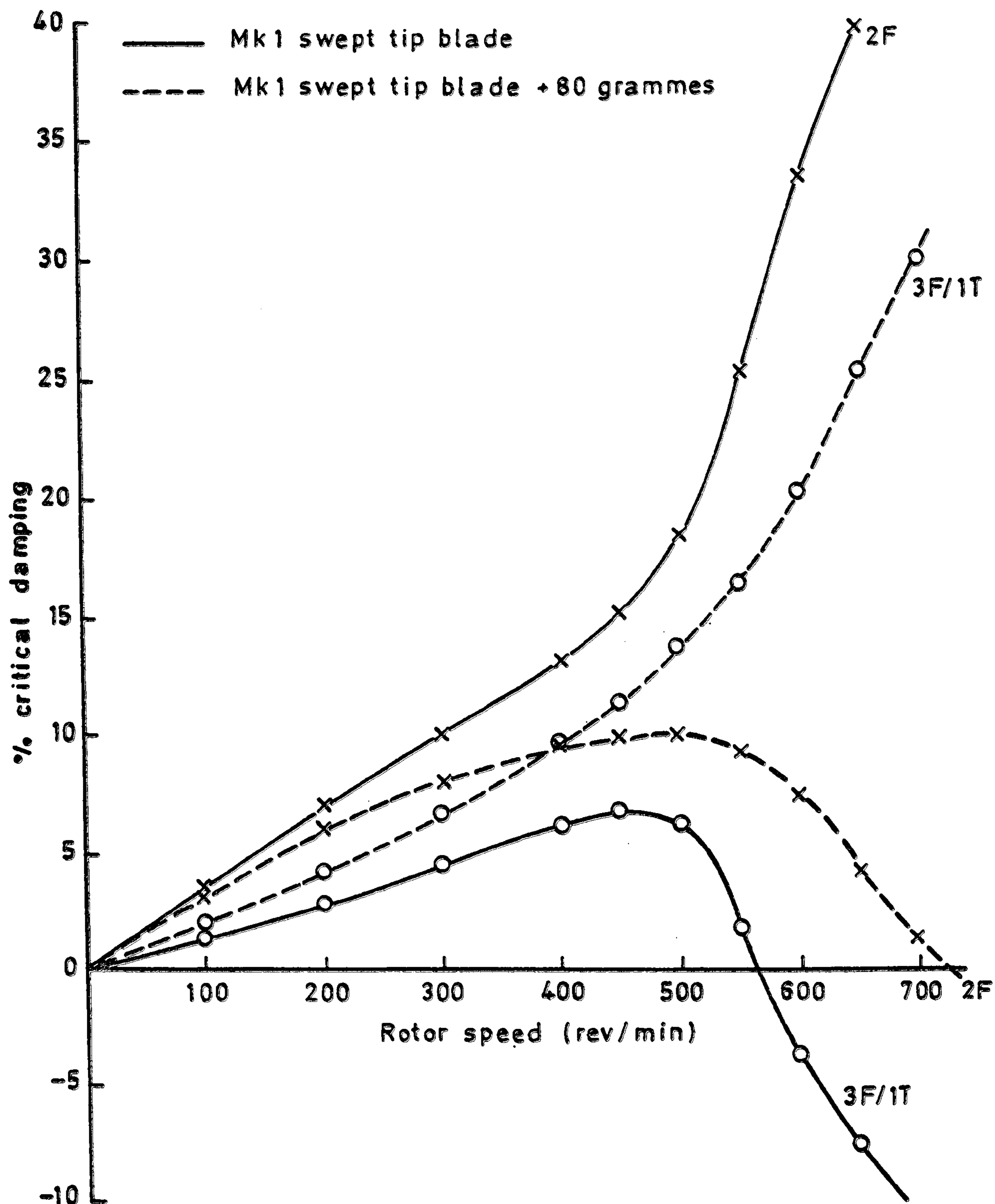


Fig 24 Plot of critical damping vs rotor speed showing the effect of adding an 80 gramme concentrated mass

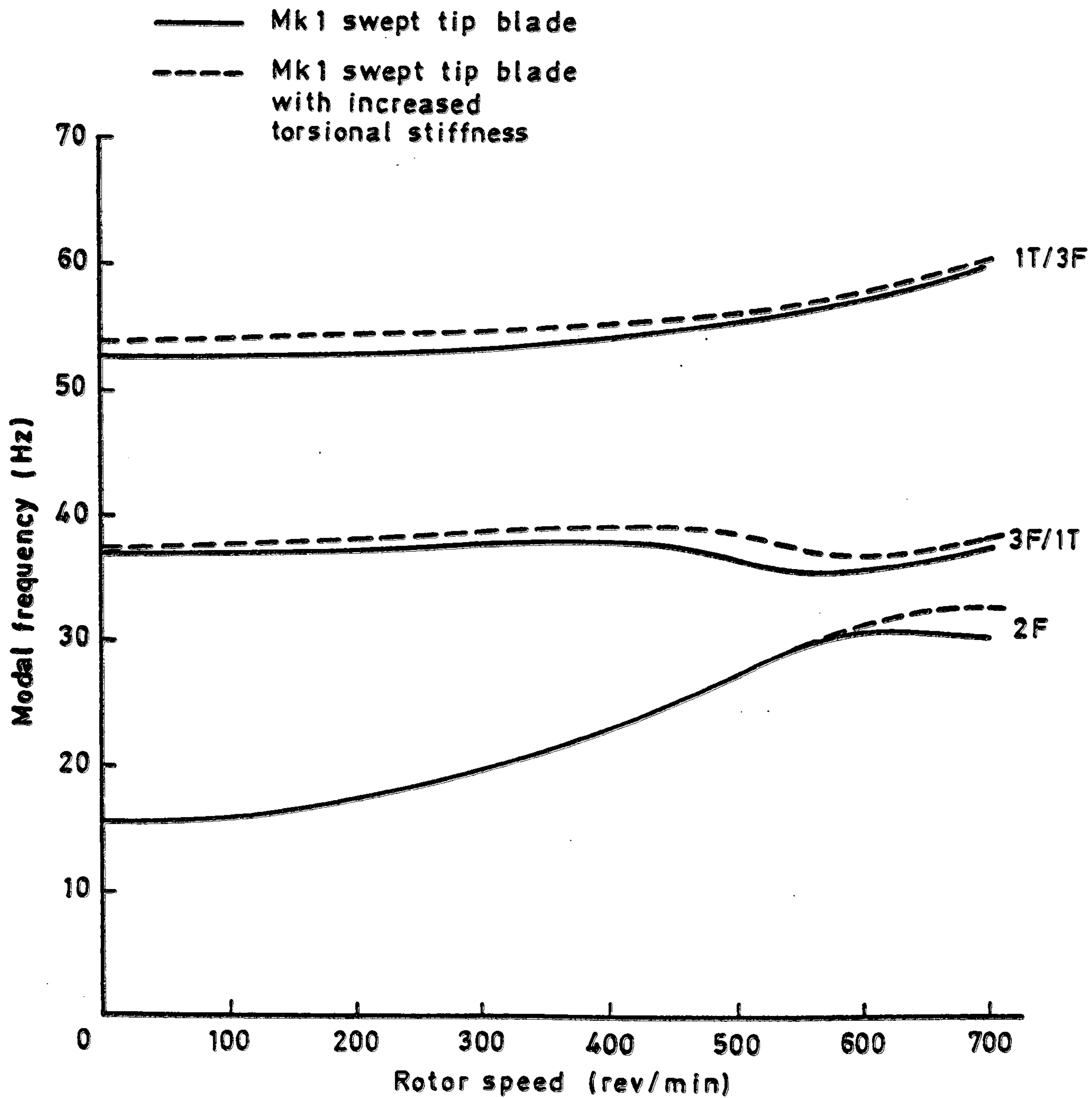


Fig 25 Plot of modal frequency vs rotor speed showing the effect of increasing the torsional stiffness by 10%

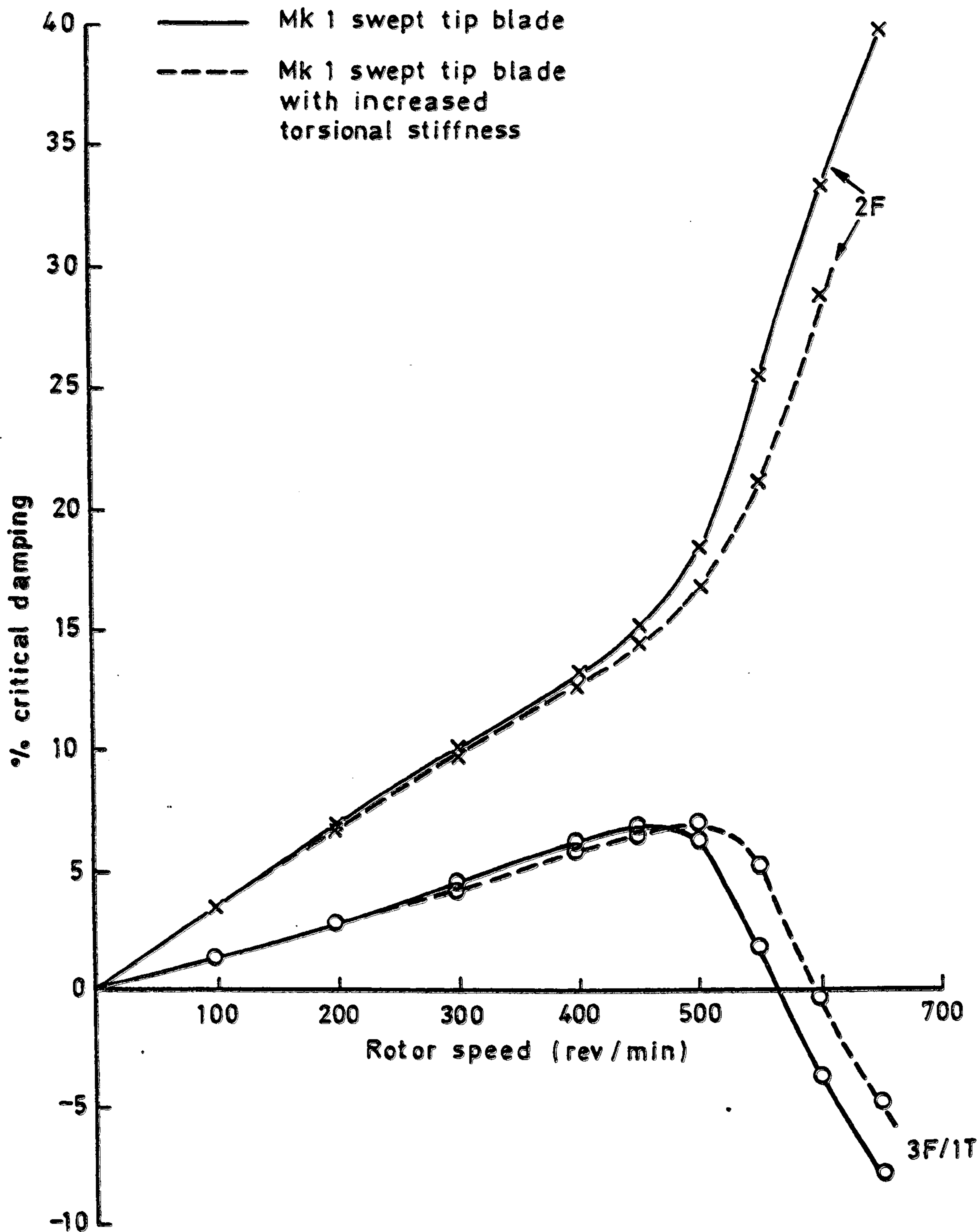


Fig 26 Plot of critical damping vs rotor speed showing the effect of increasing the torsional stiffness by 10%

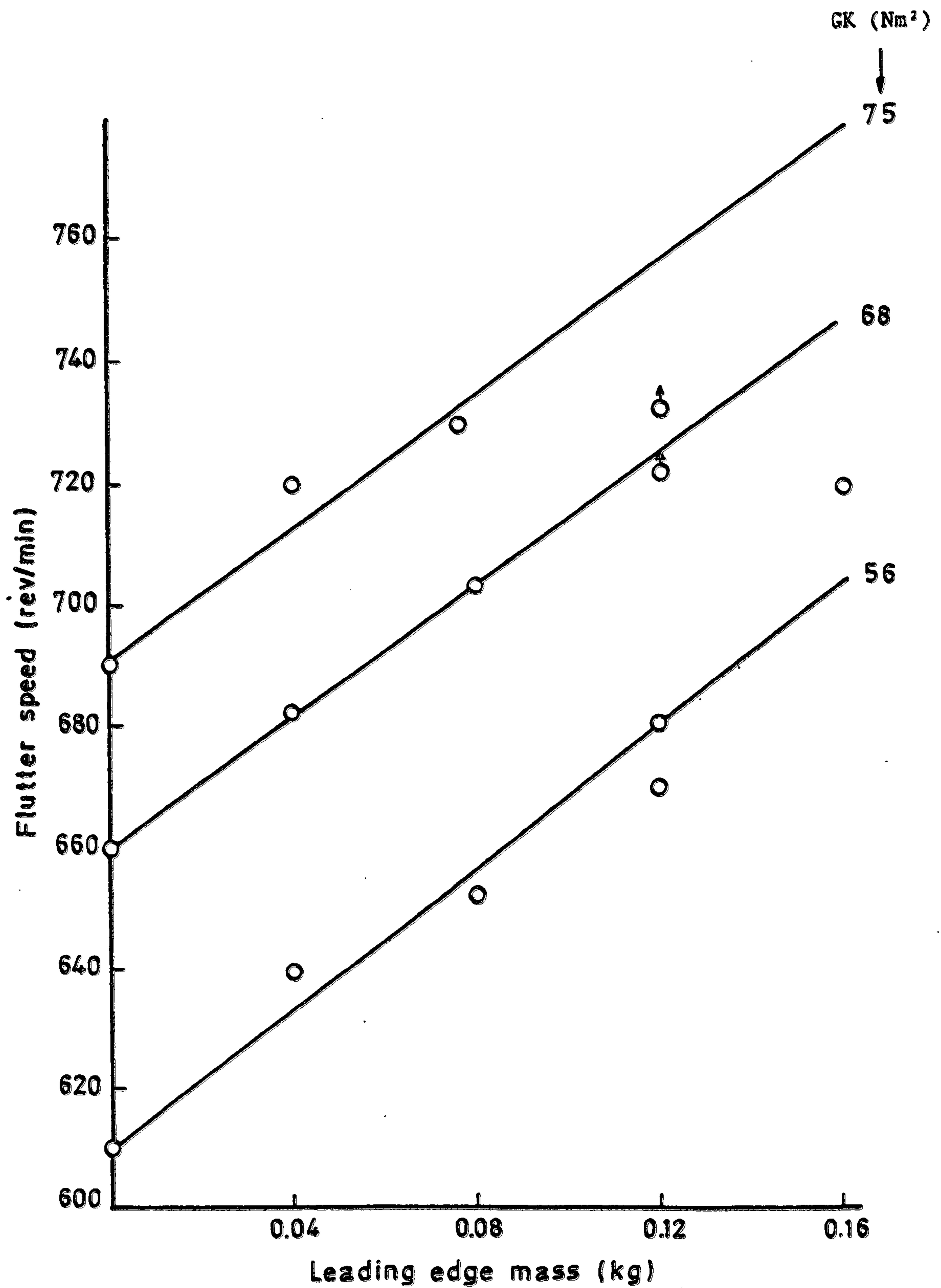


Fig 27 Effect of mass and torsional stiffness on flutter speed

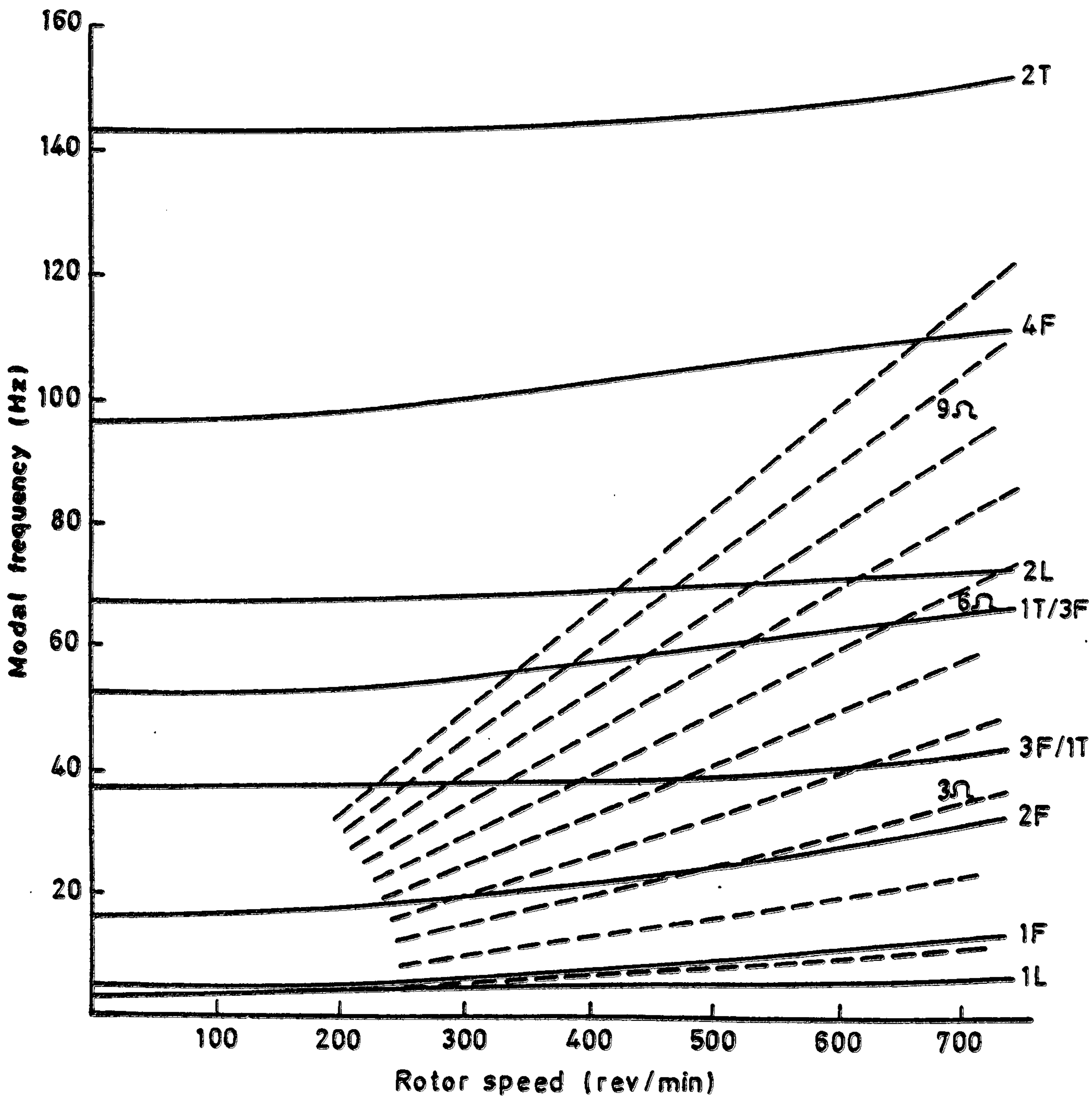


Fig 28 Spoke diagram of the undamped modal frequencies for the Mk2 swept tip blade

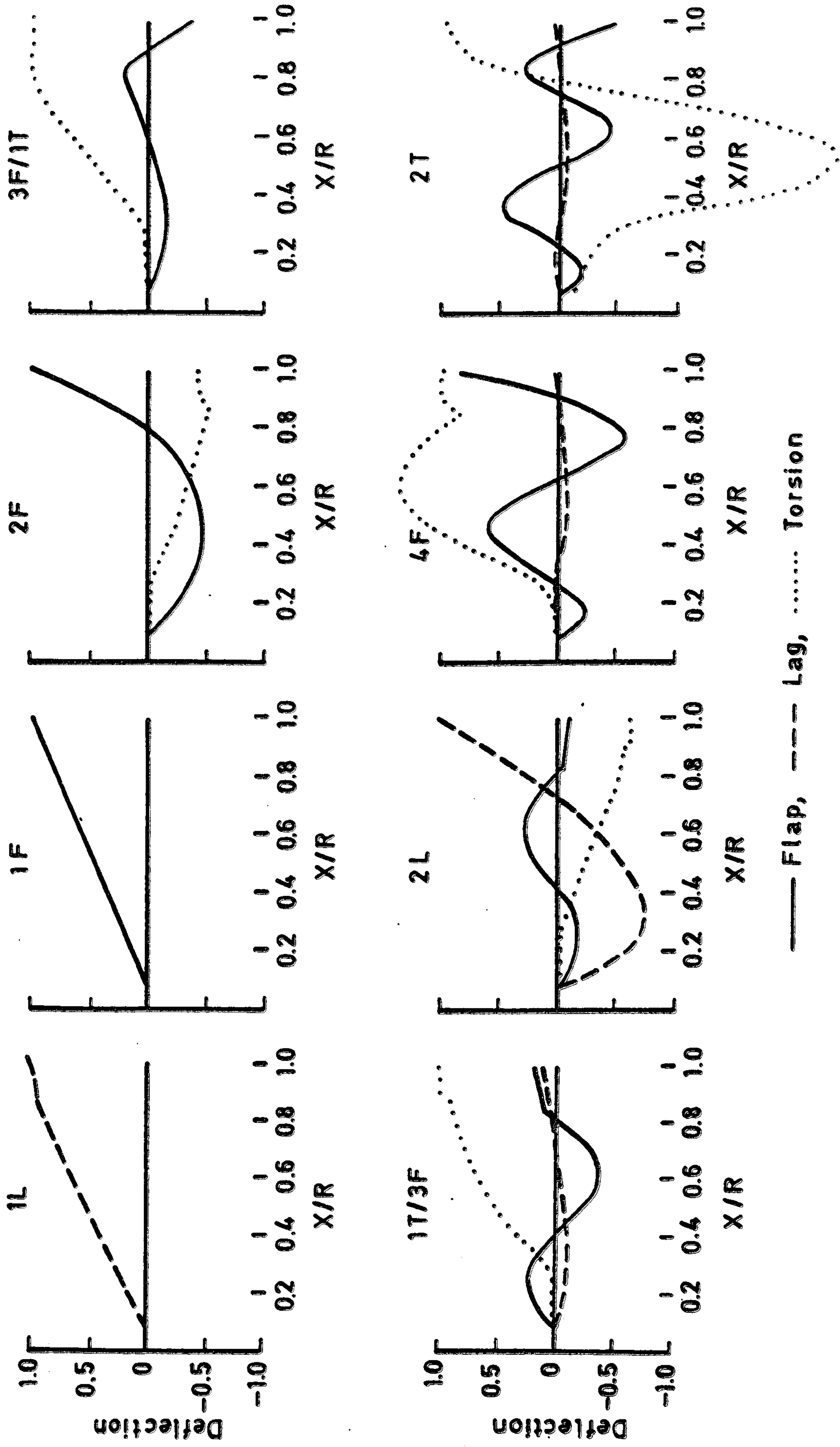


Fig 29 Mode shapes for the Mk2 swept tip blade (600 rev/min)

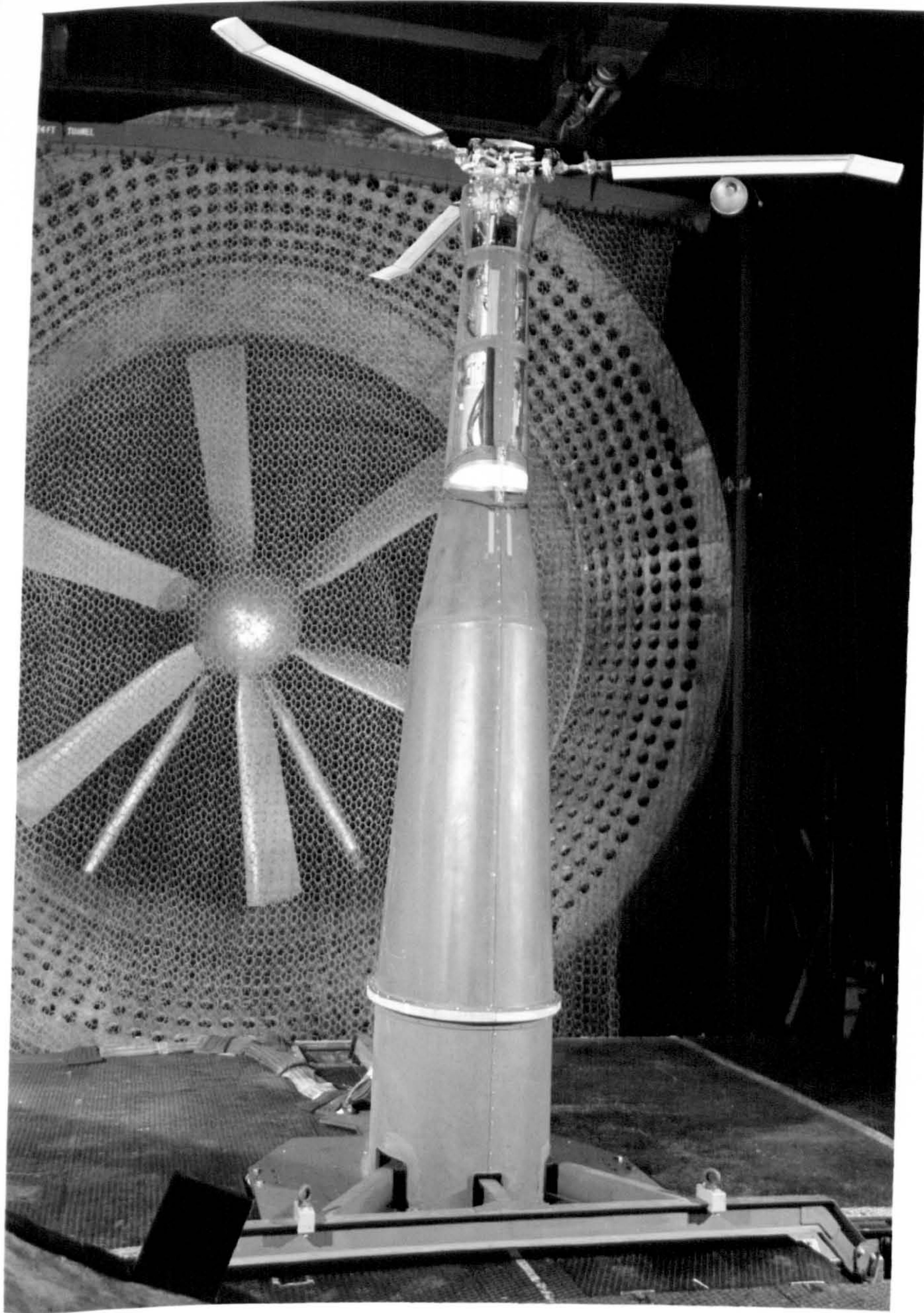


Fig 30 Rotor test rig installed in the RAE 24ft wind tunnel

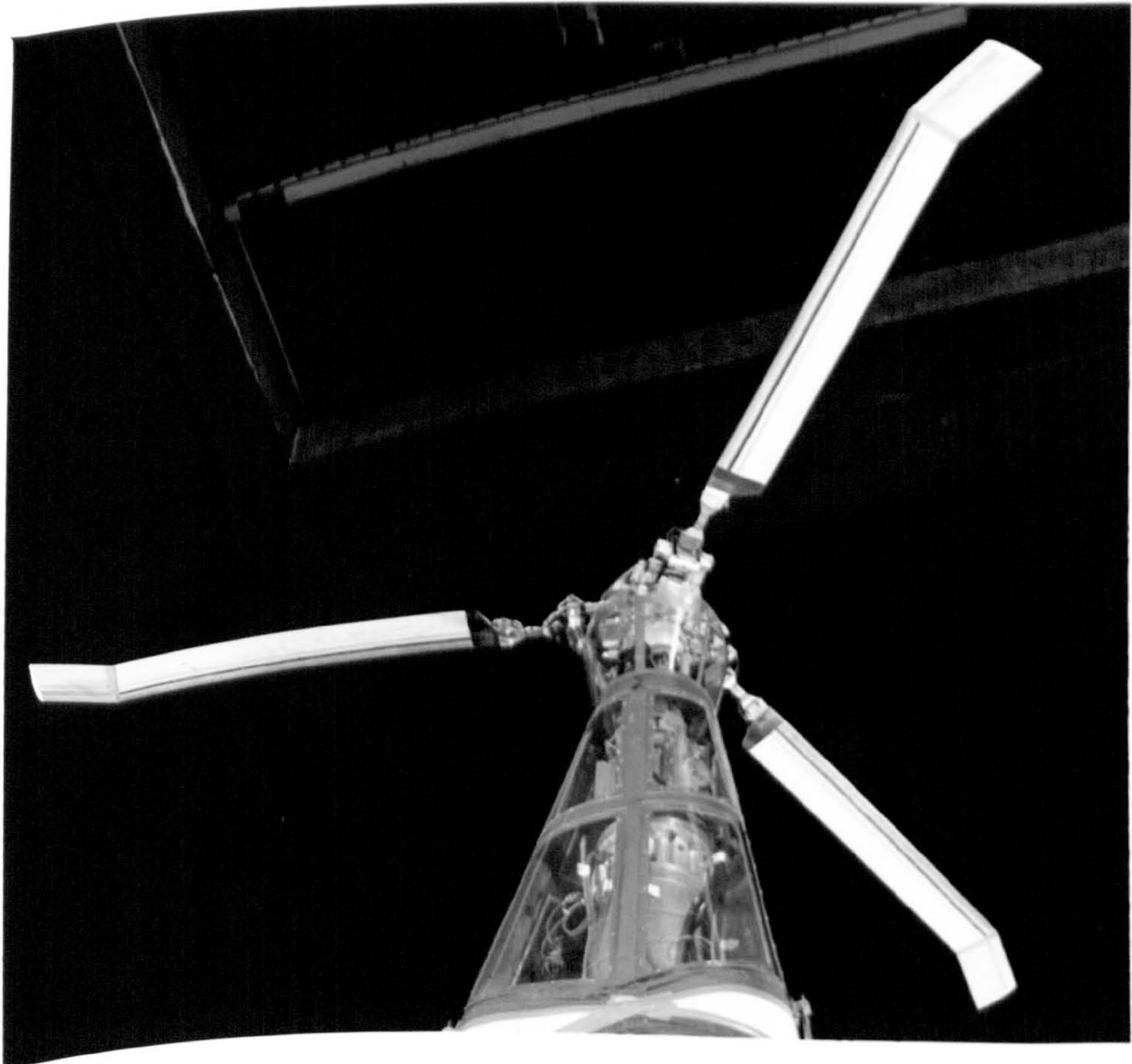


Fig 31 The swept tip blade mounted on the dual load path hub

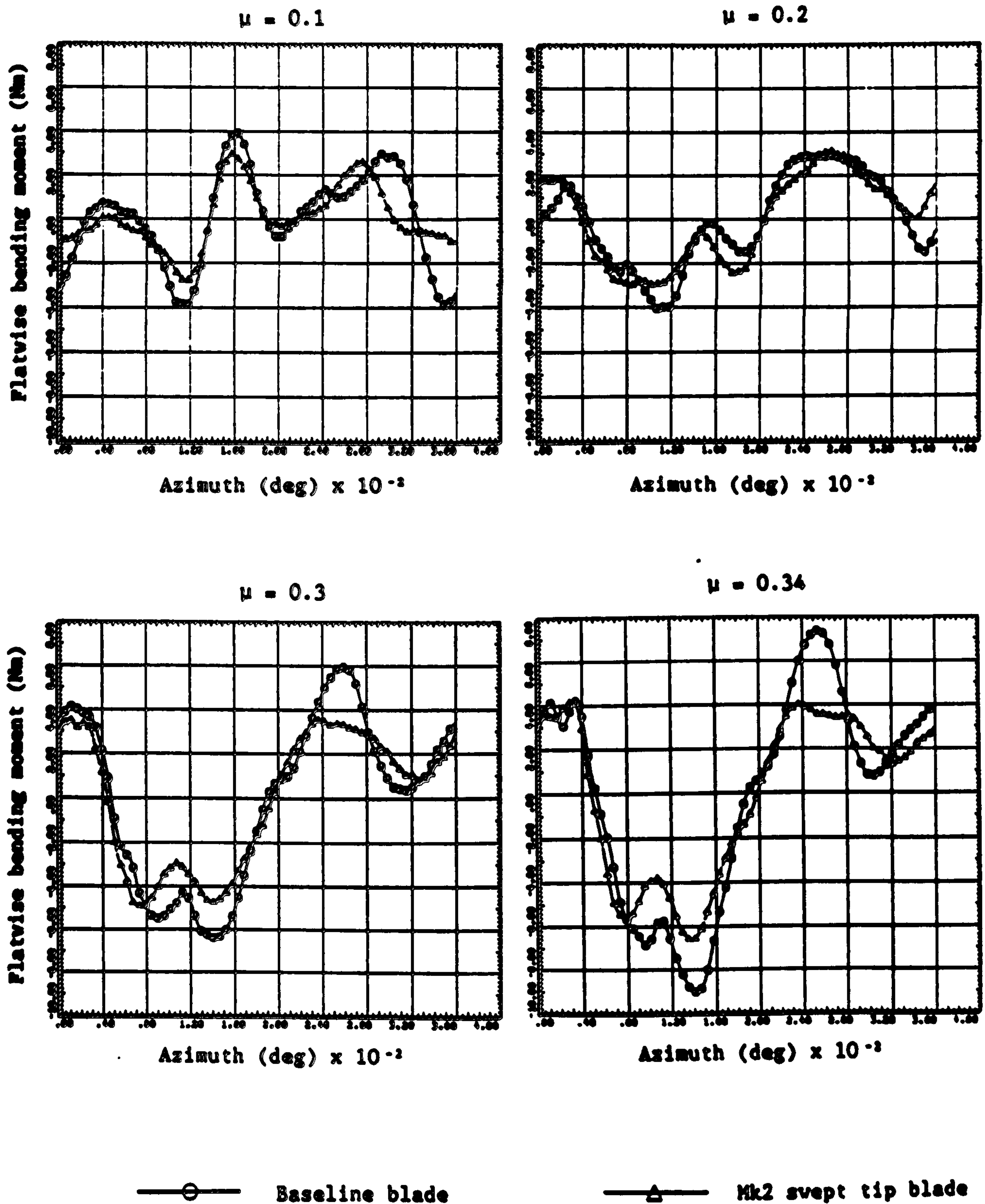


Fig 32 Measured flatwise bending moments on the rectangular and Mk2 swept tip blades, thrust = 900N, 30% R

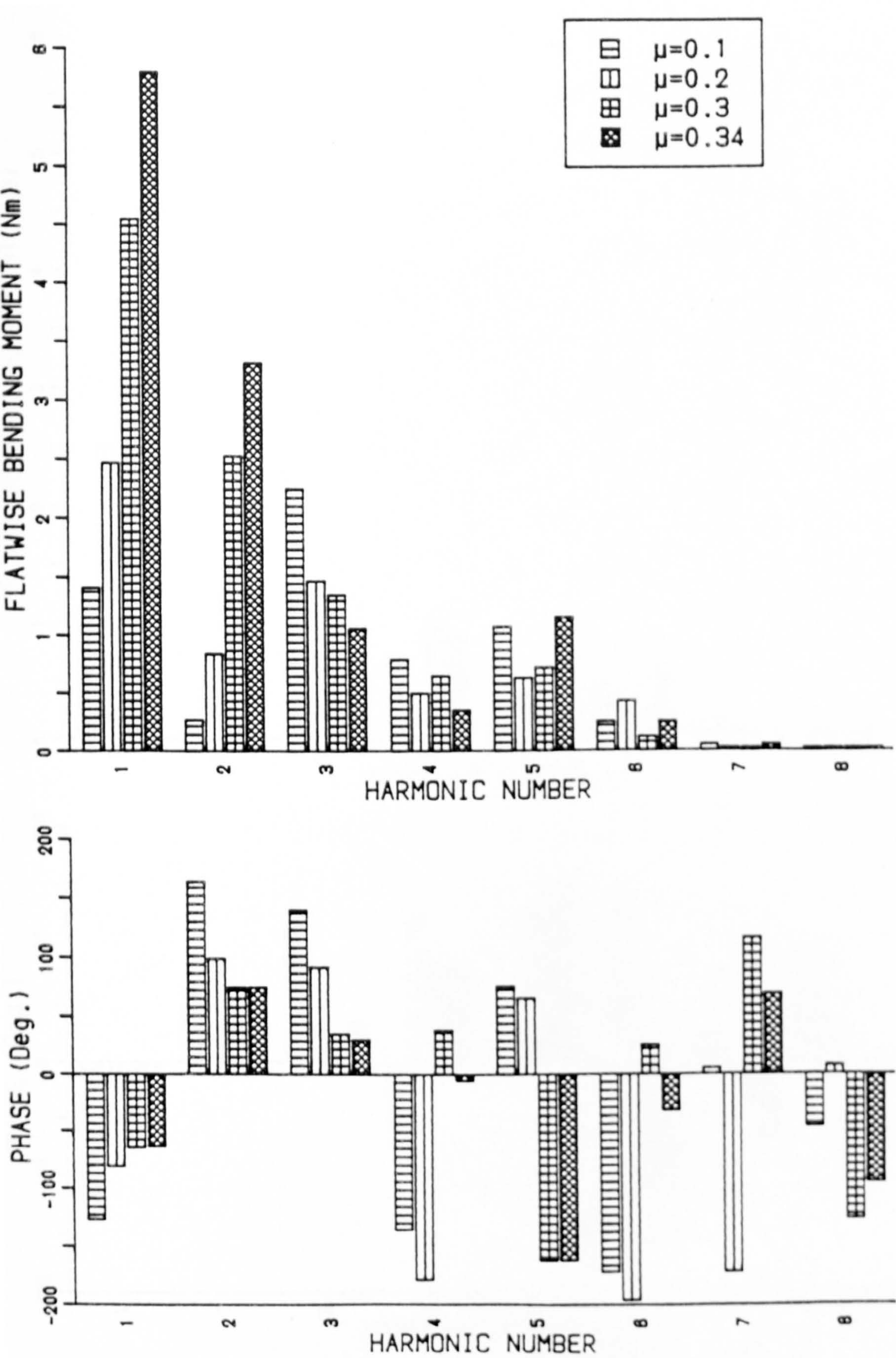


Fig 33 Harmonic analysis of the measured flatwise bending moments on the baseline blade, thrust = 900N, 30% R

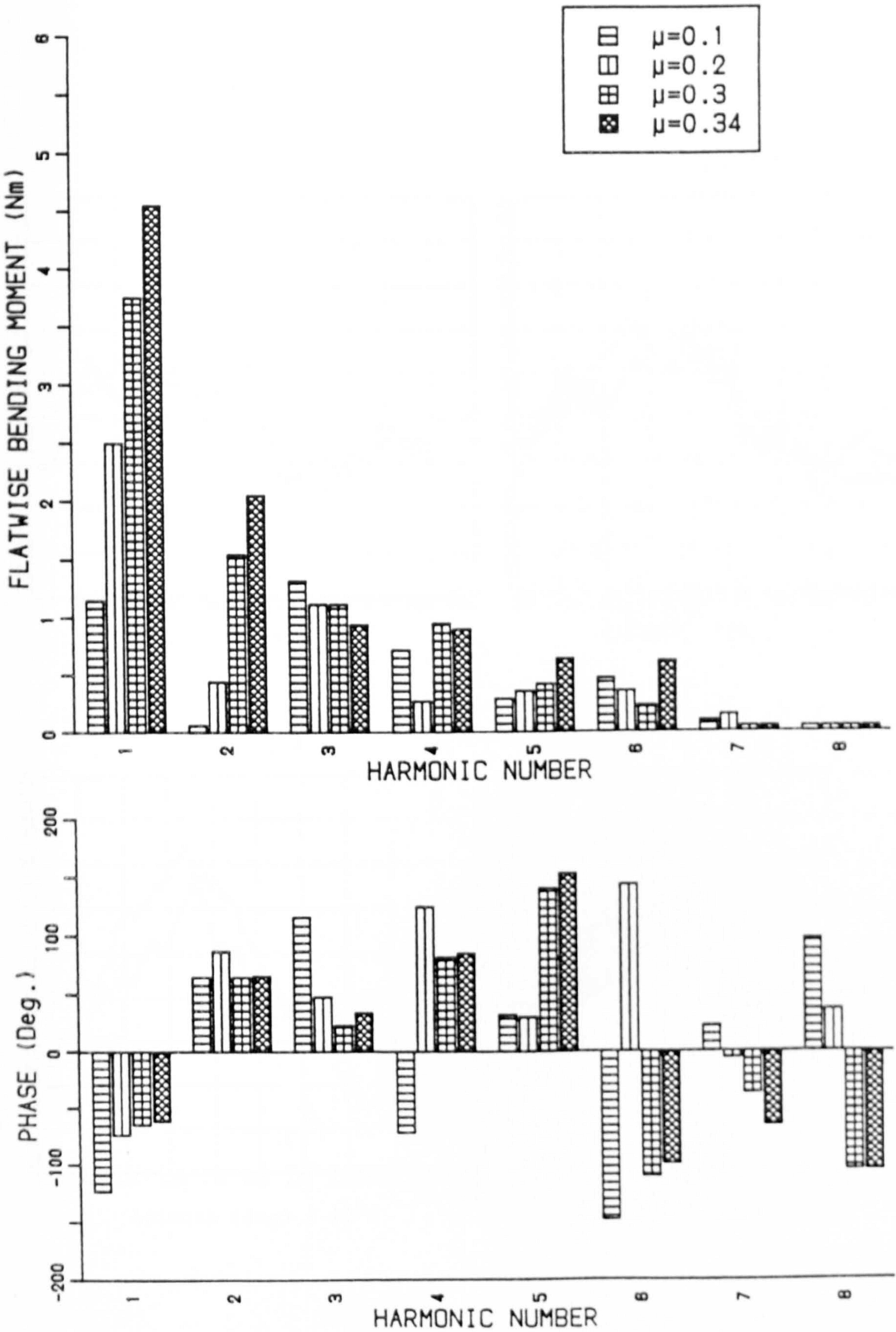
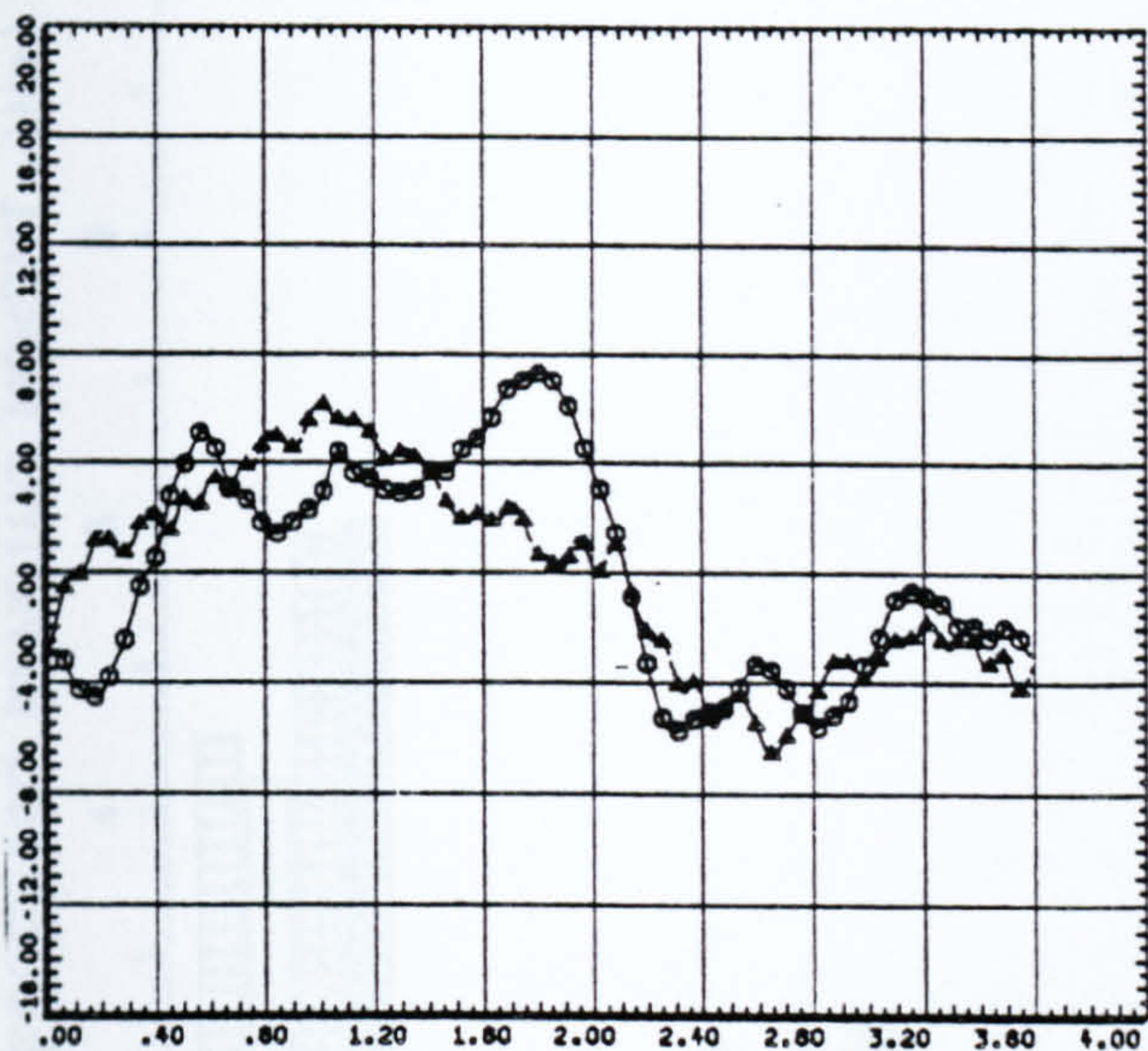


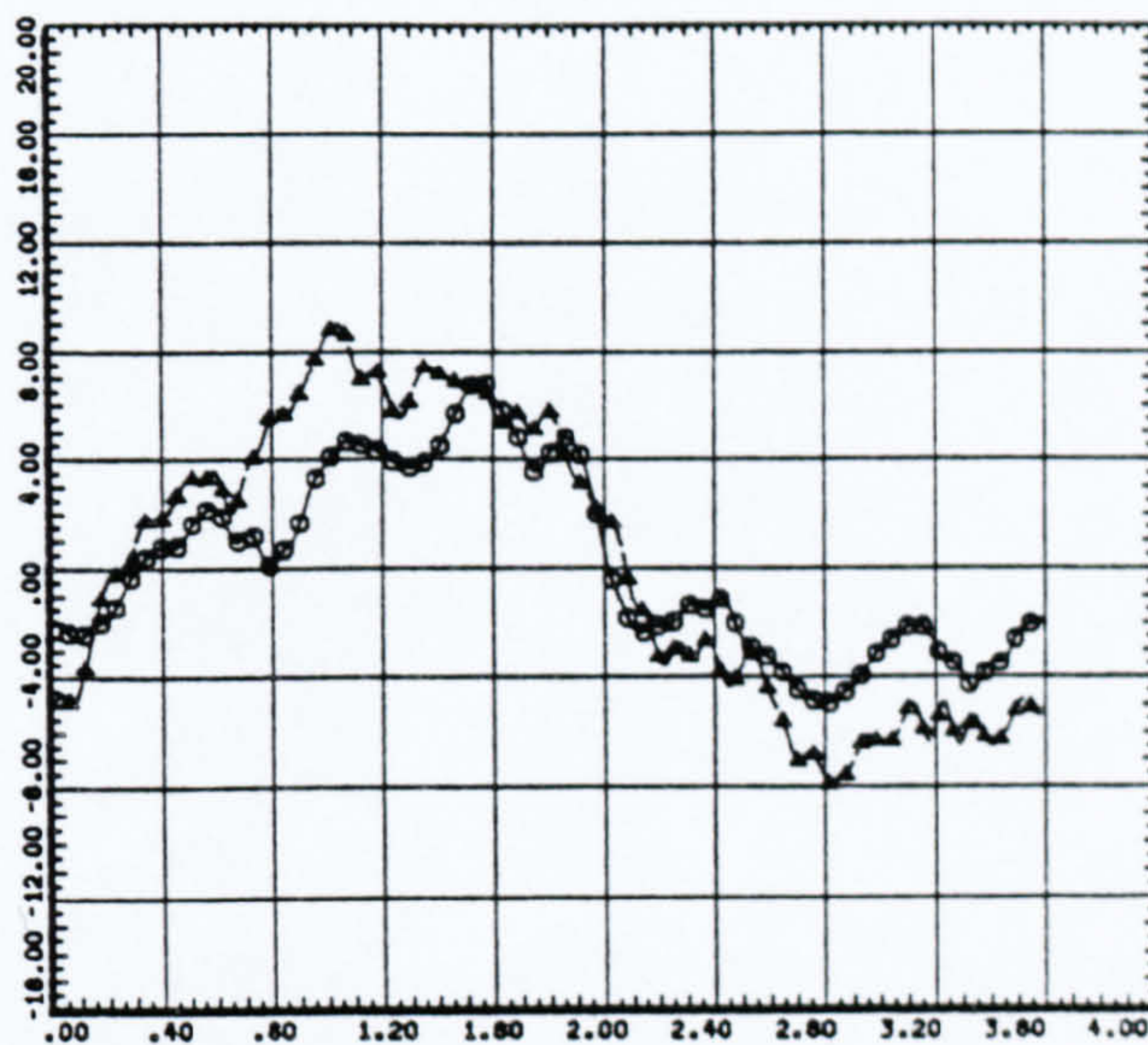
Fig 34 Harmonic analysis of the measured flatwise bending moments on the Mk2 swept tip blade, thrust = 900N, 30% R

$\mu = 0.1$



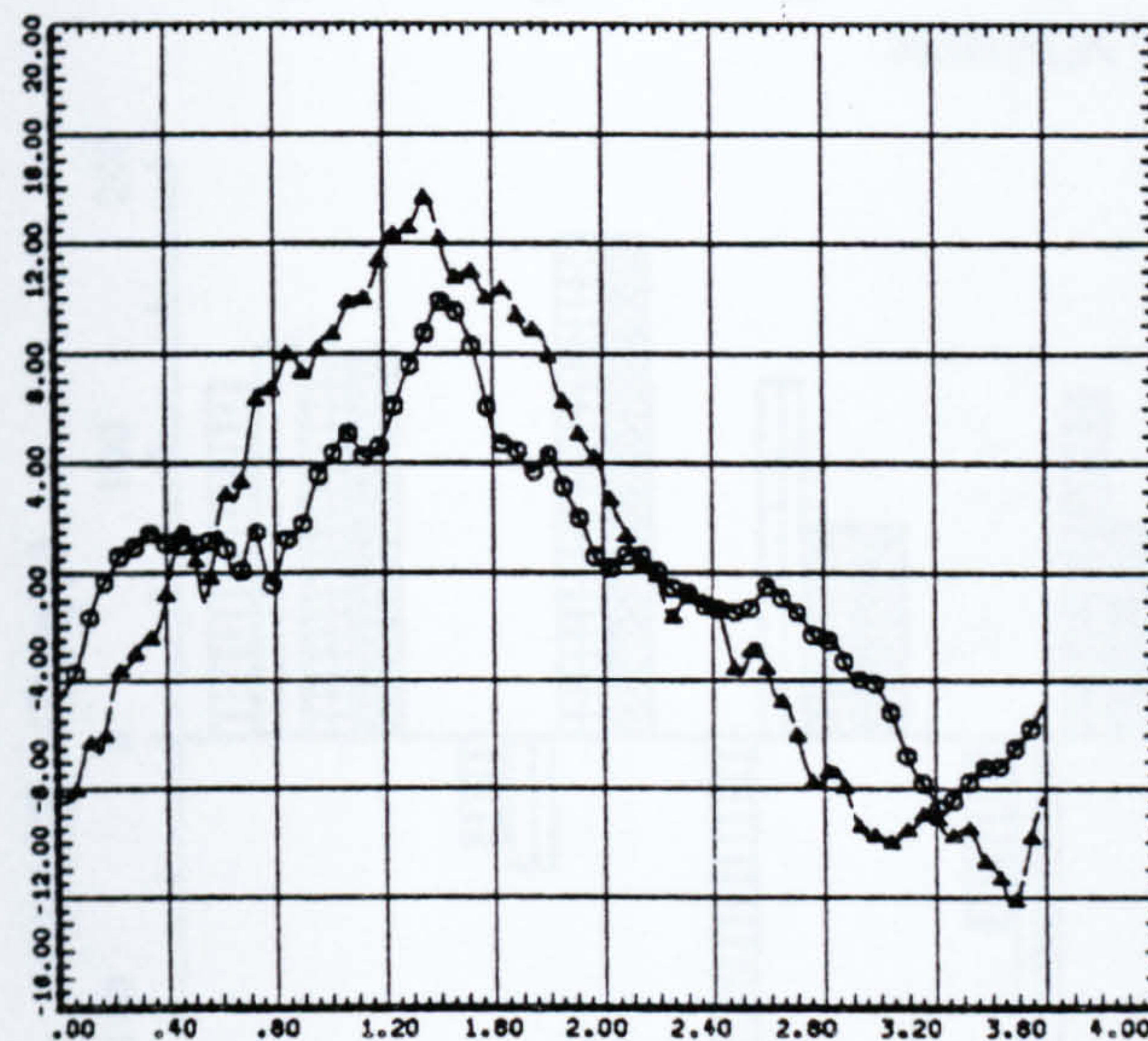
Azimuth (deg) $\times 10^{-2}$

$\mu = 0.2$



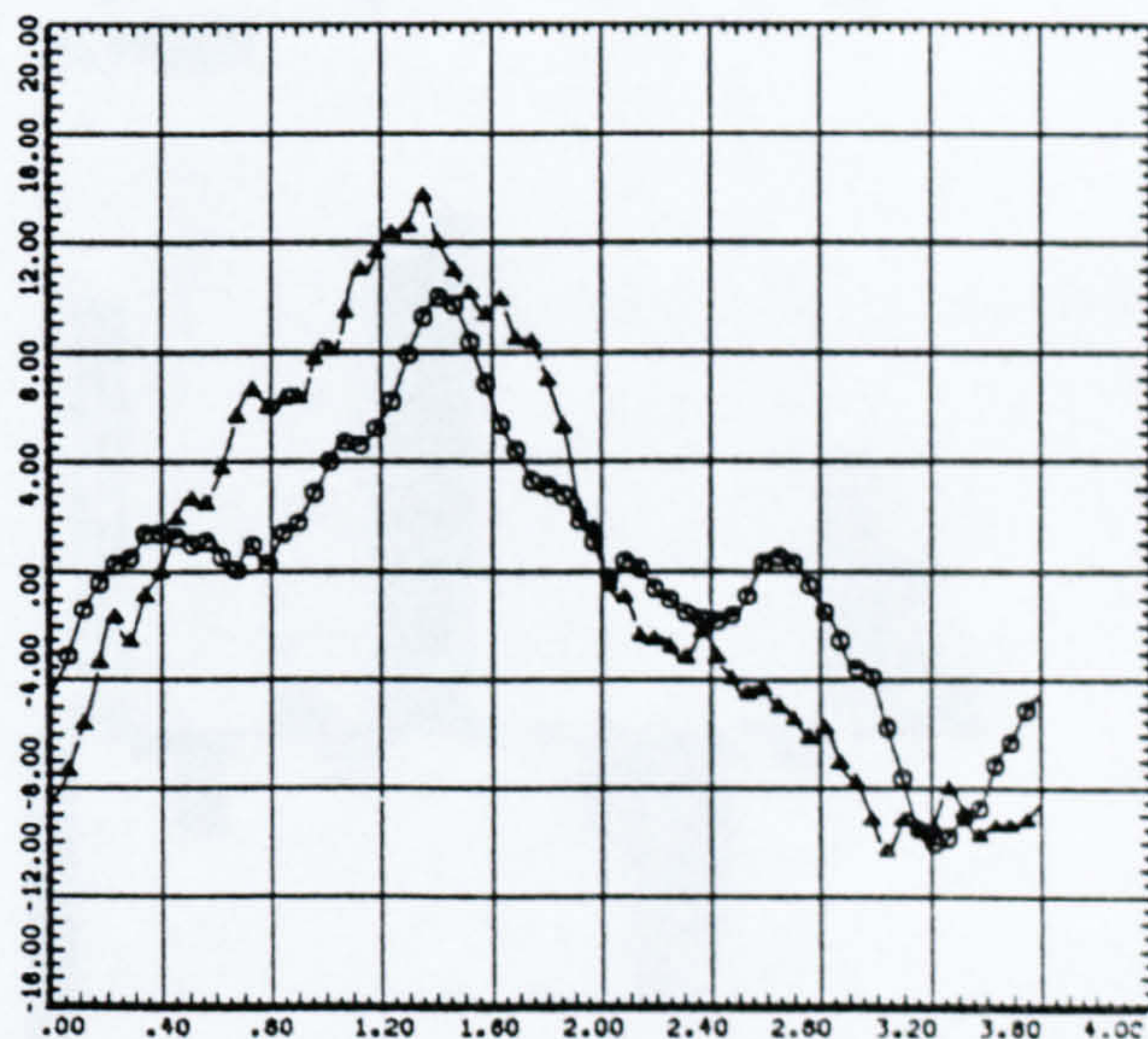
Azimuth (deg) $\times 10^{-2}$

$\mu = 0.3$



Azimuth (deg) $\times 10^{-2}$

$\mu = 0.34$



Azimuth (deg) $\times 10^{-2}$



Baseline blade



Mk2 swept tip blade

Fig 35 Measured edgewise bending moments on the rectangular and Mk2 swept tip blades, thrust = 900N, 37% R

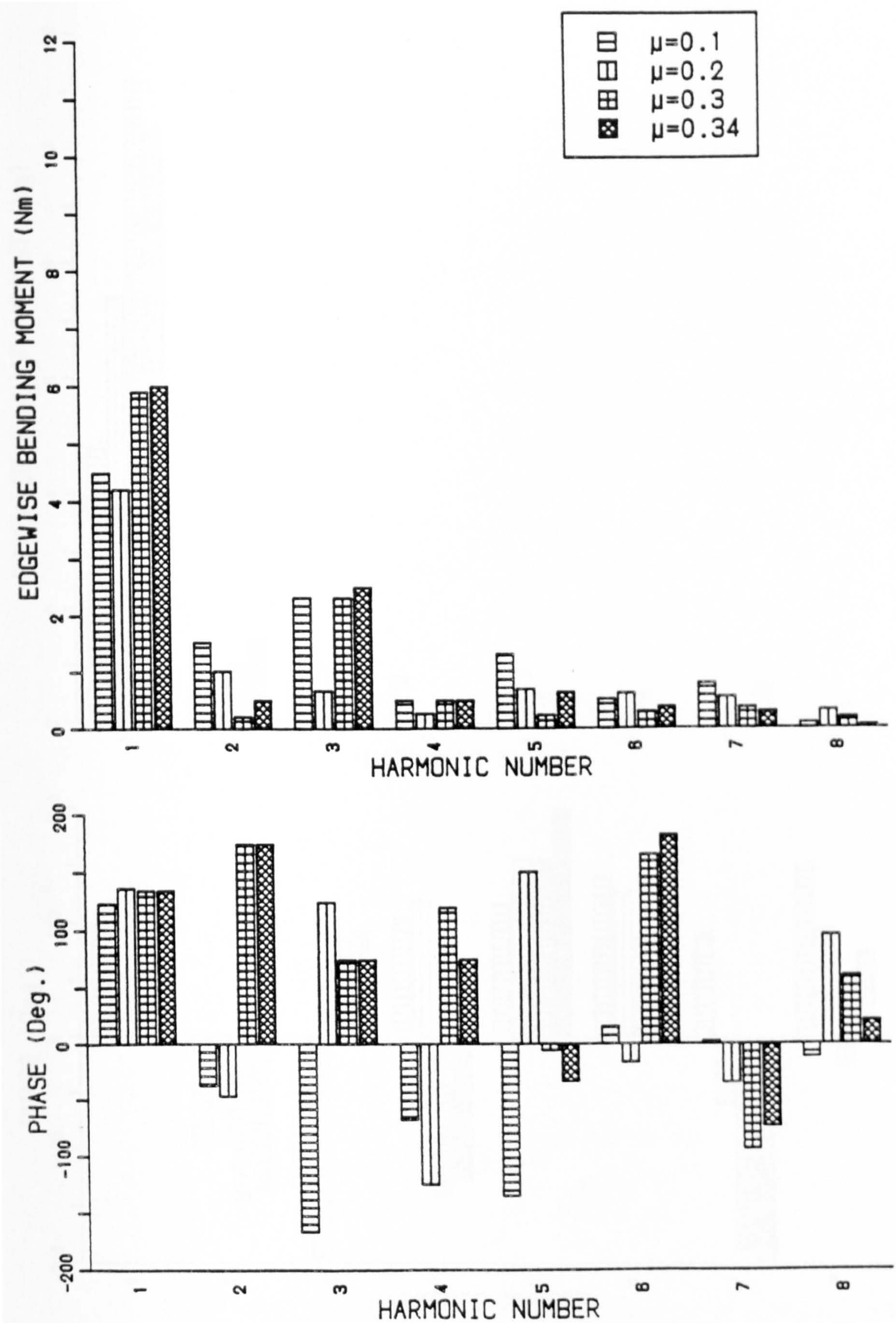


Fig 36 Harmonic analysis of the measured edgewise bending moments on the baseline blade, thrust = 900N, 37% R

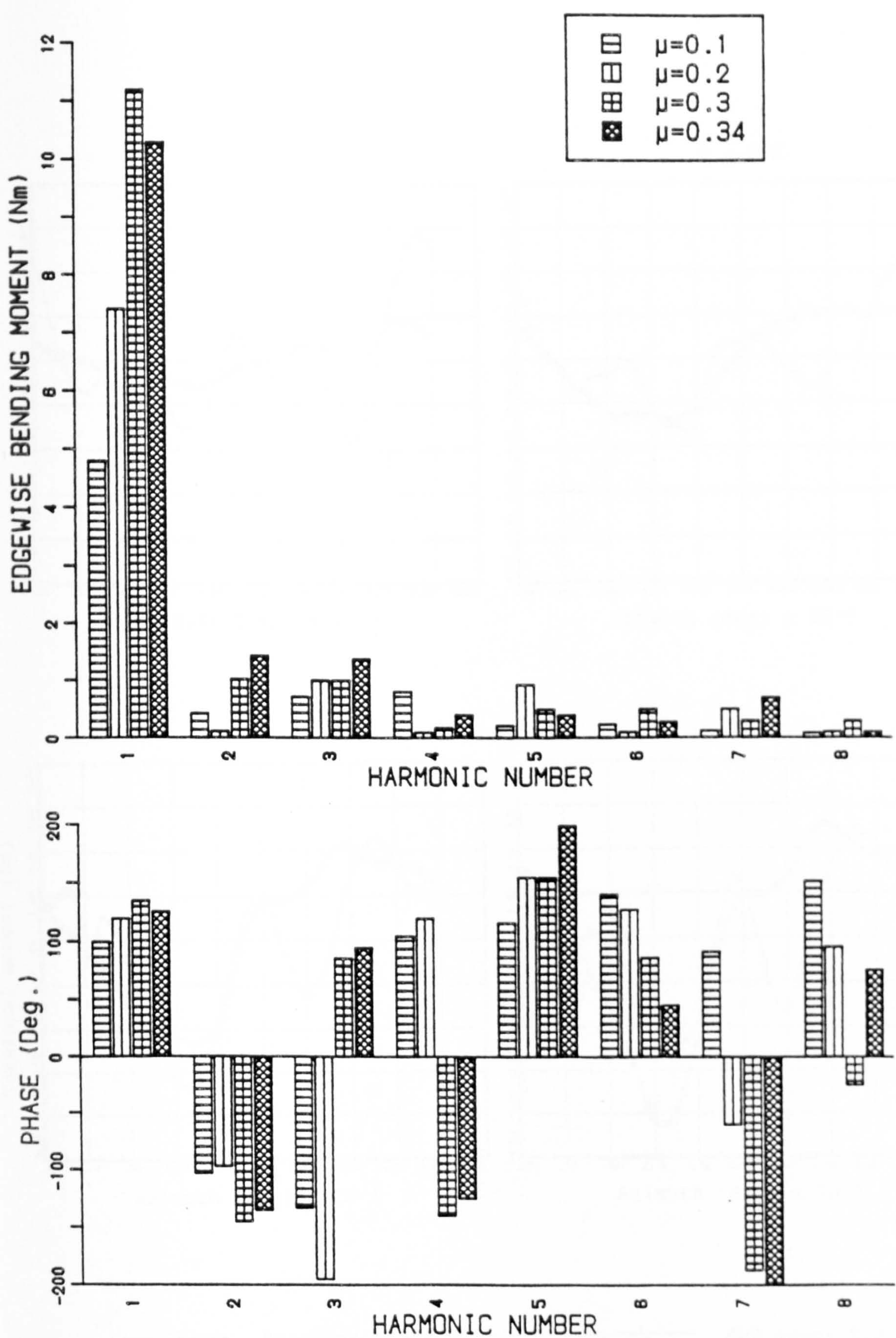


Fig 37 Harmonic analysis of the measured edgewise bending moments on the Mk2 swept tip blade, thrust = 900N, 37% R

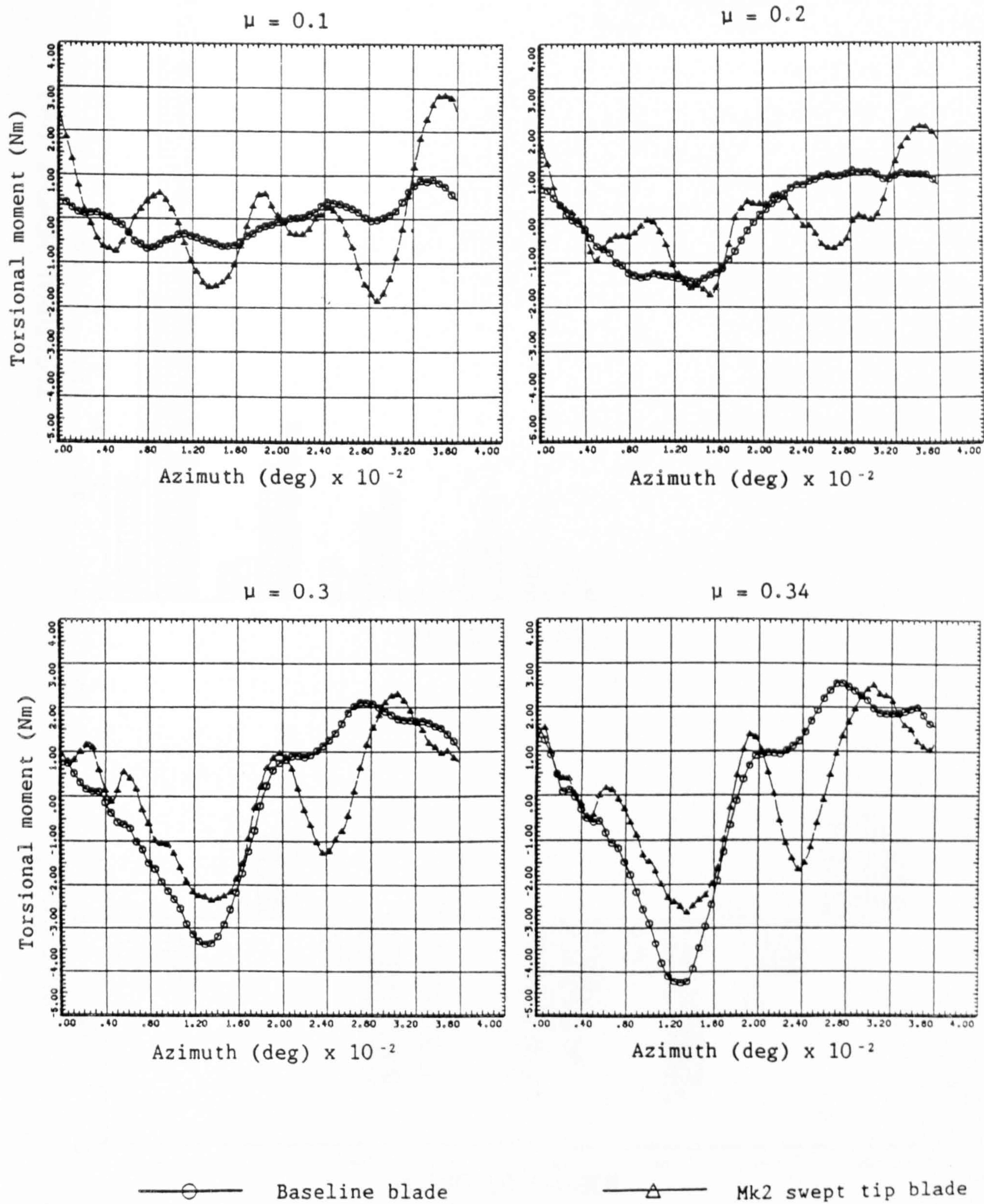


Fig 38 Measured torsional moments on the rectangular and Mk2 swept tip blades, thrust = 900N, 30% R

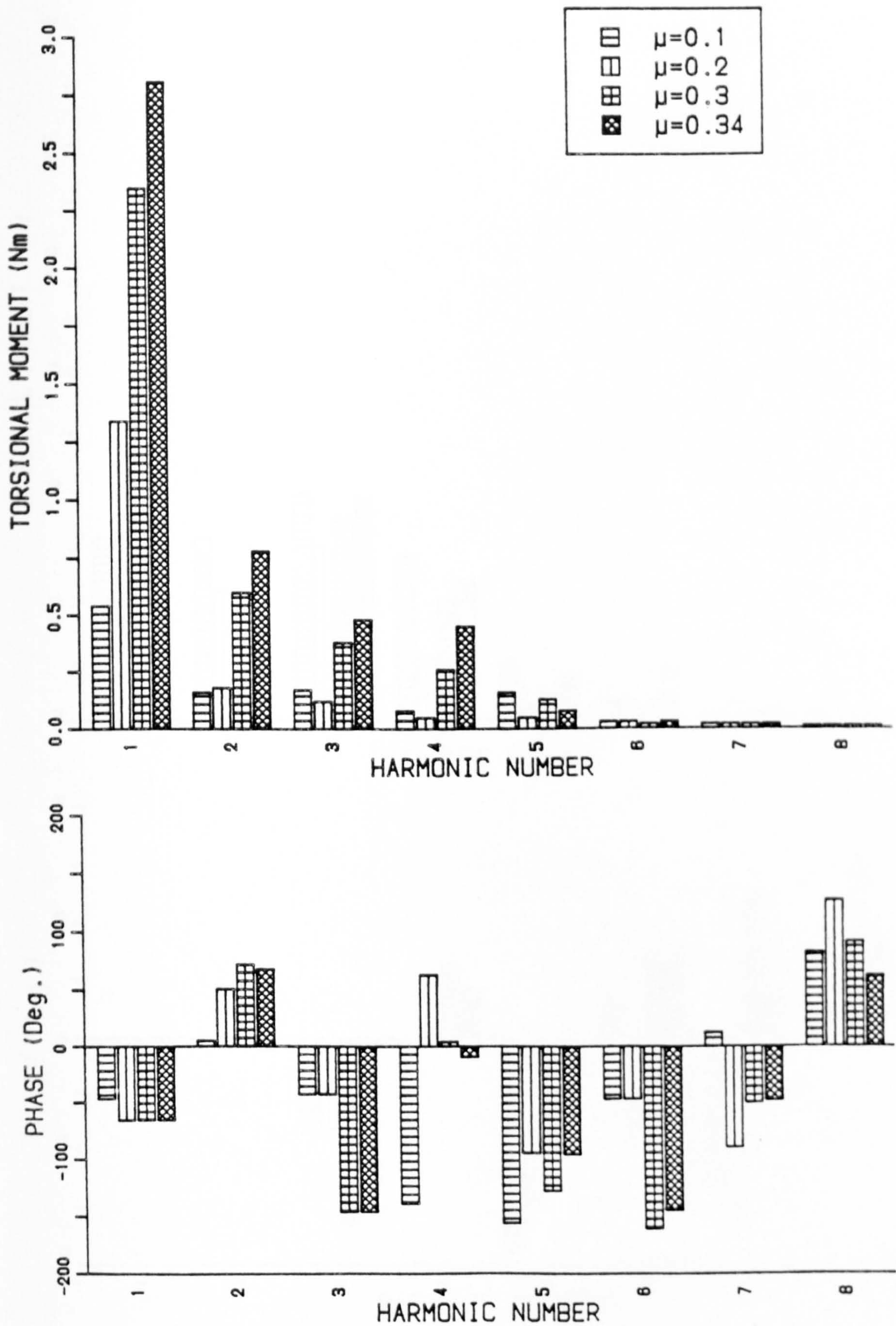


Fig 39 Harmonic analysis of the measured torsional moments on the baseline blade, thrust = 900N, 30% R

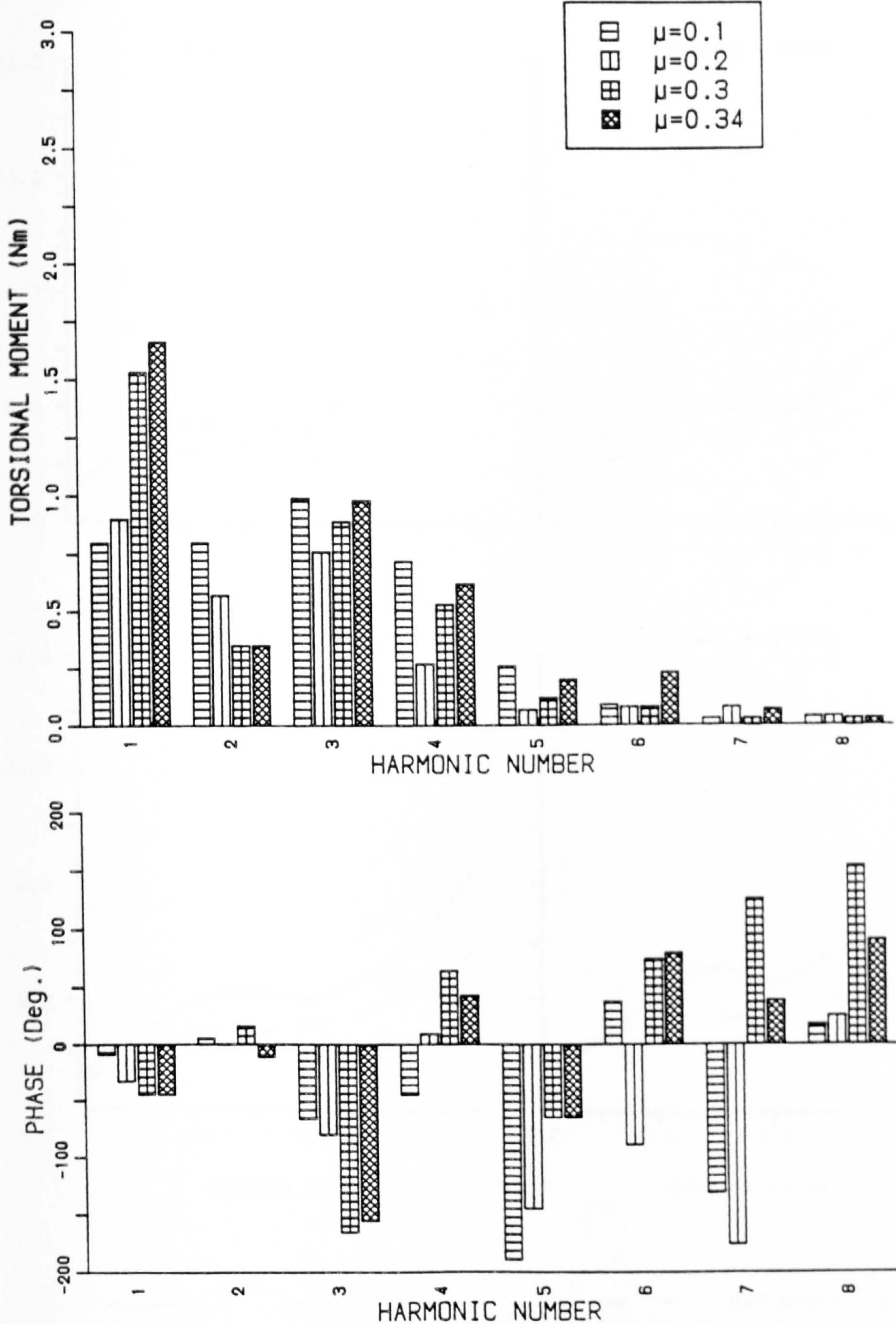


Fig 40 Harmonic analysis of the measured torsional moments on the Mk2 swept tip blade, thrust = 900N, 30% R

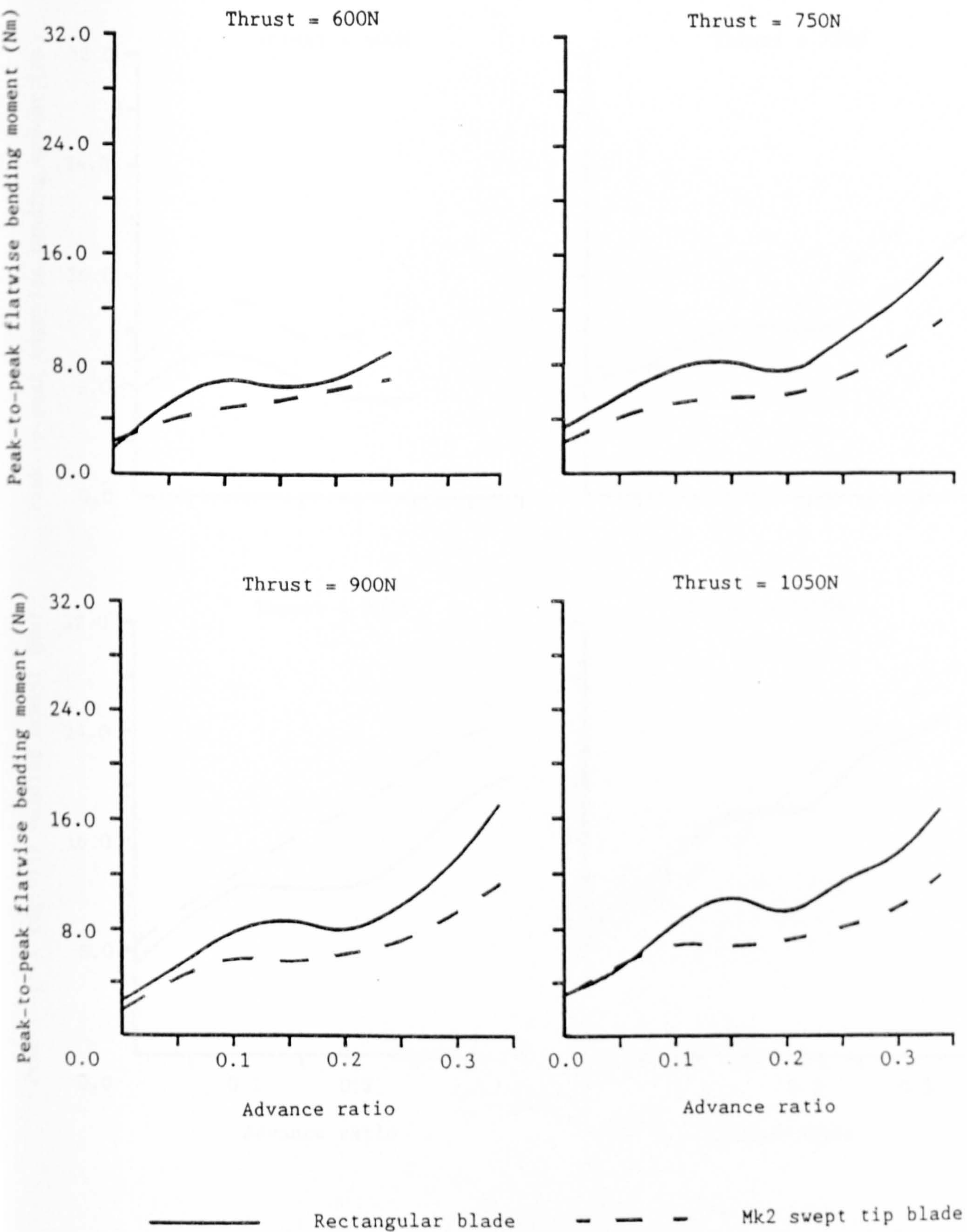


Fig 41 Measured peak-to-peak flatwise bending moments - 30% R

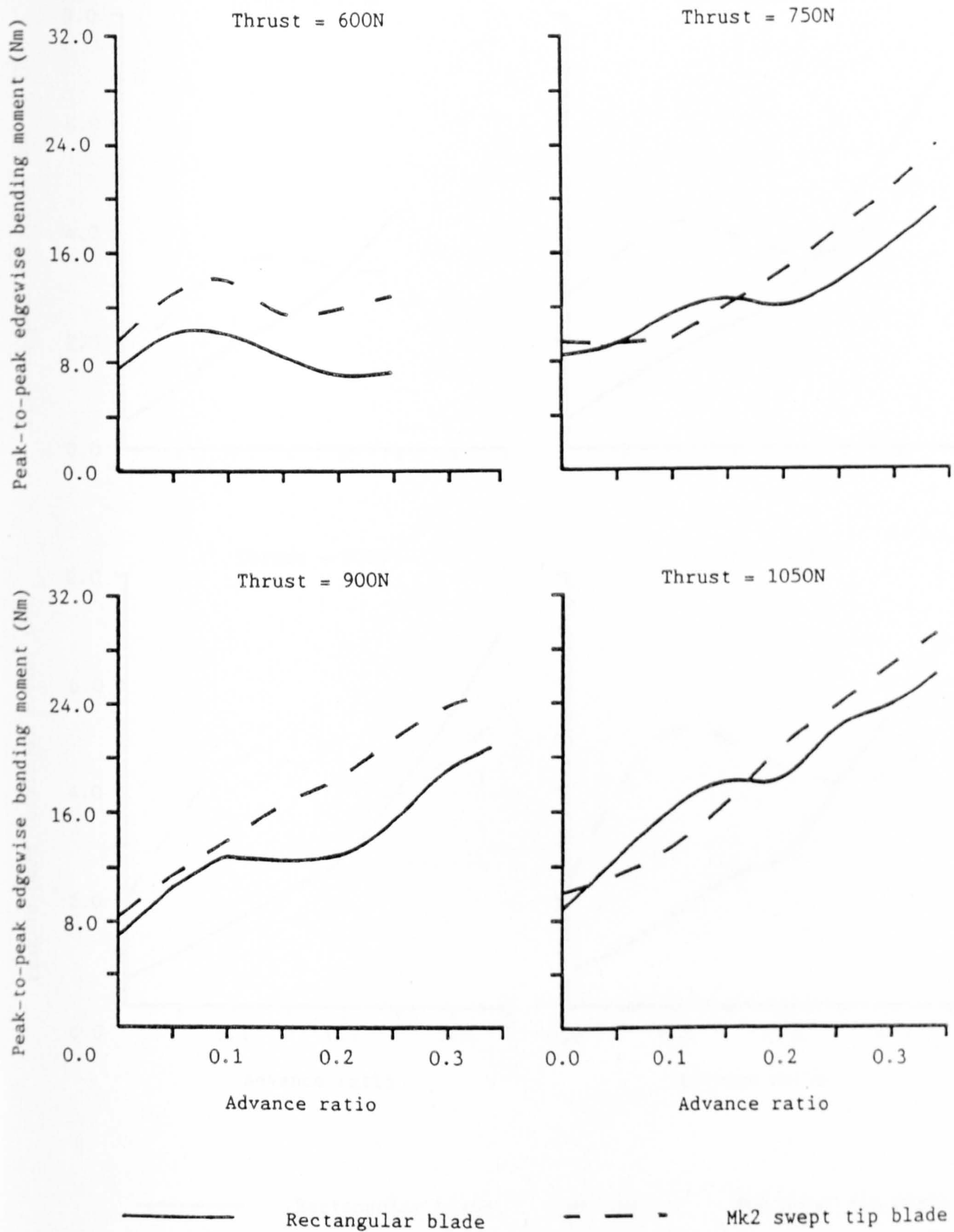


Fig 42 Measured peak-to-peak edgewise bending moments - 37% R

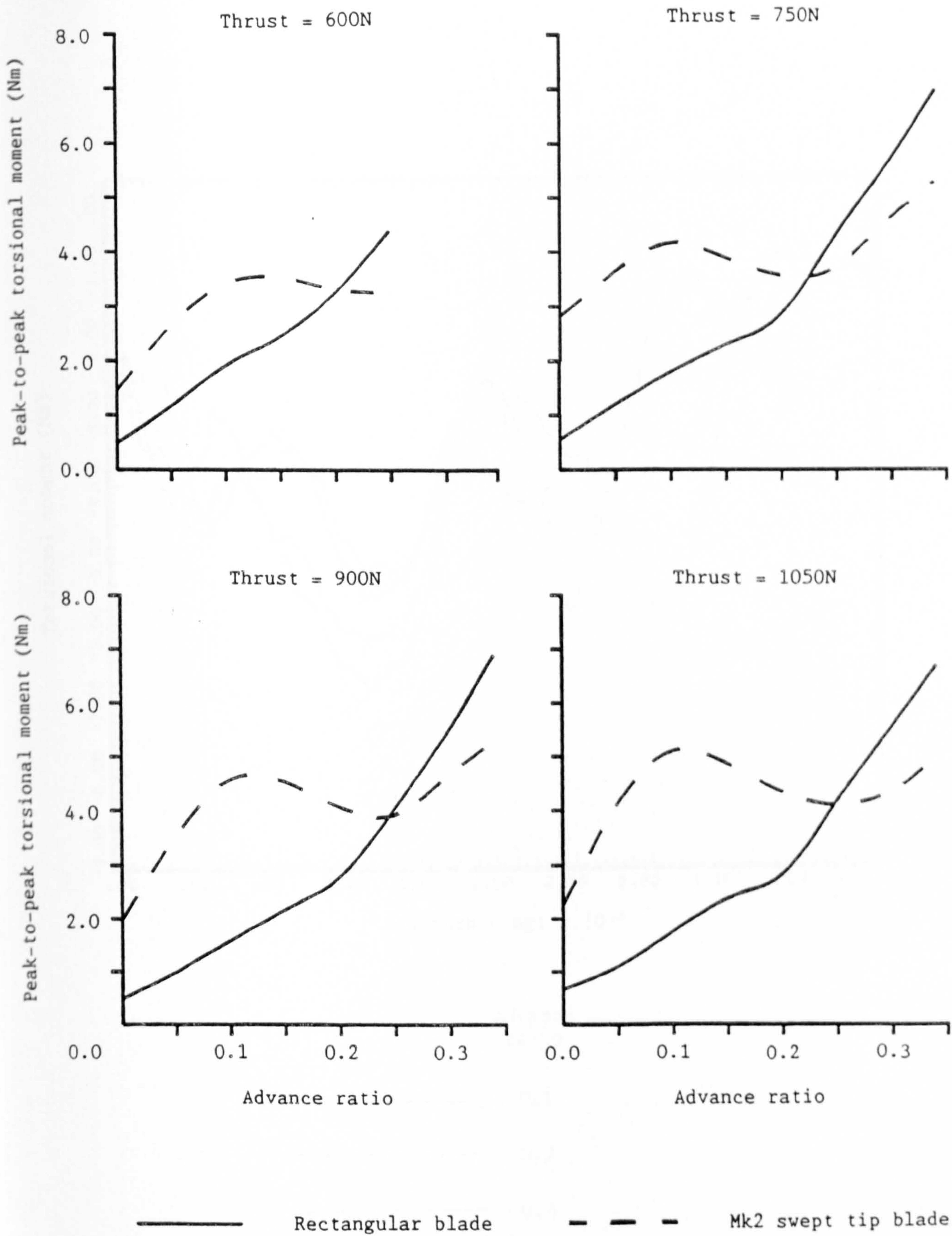


Fig 43 Measured peak-to-peak torsional moments - 30% R

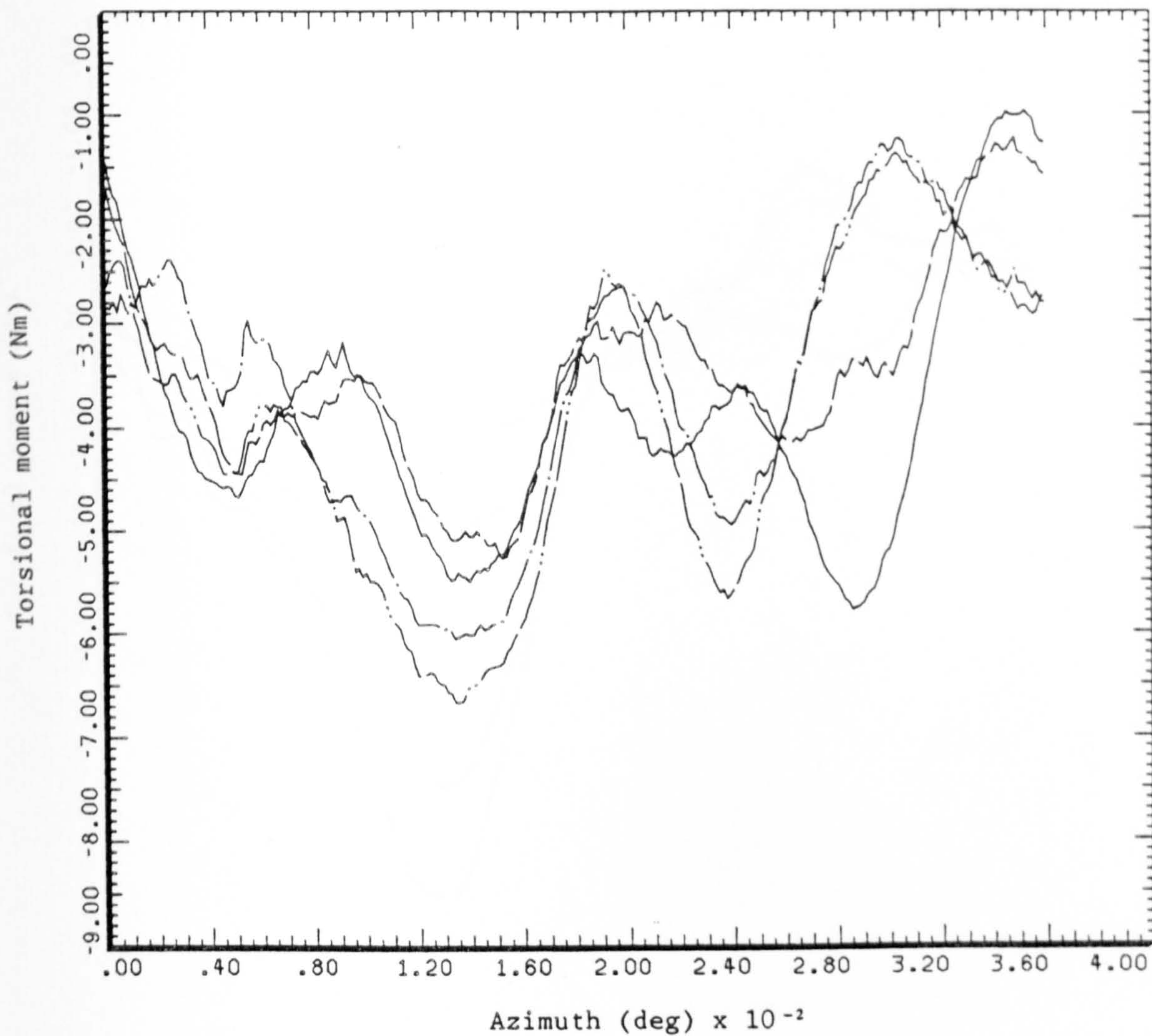
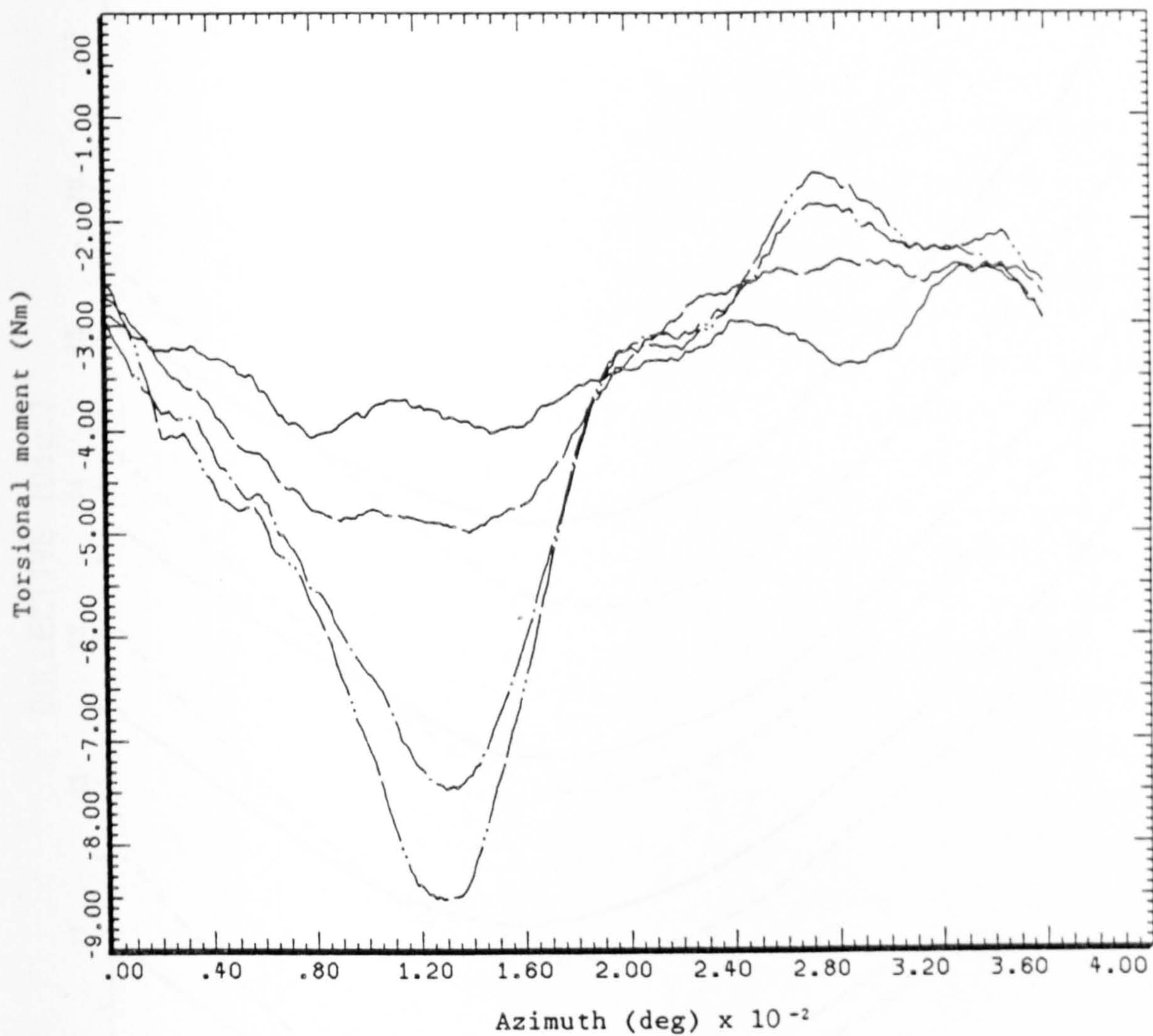


Fig 44 Measured torsional moment at the root of the swept tip blade, thrust = 900N



Advance ratio	
—————	0.1
- - - - -	0.2
- . - . -	0.3
- - - - -	0.34

Fig 45 Measured torsional moment at the root of the rectangular blade, thrust = 900N

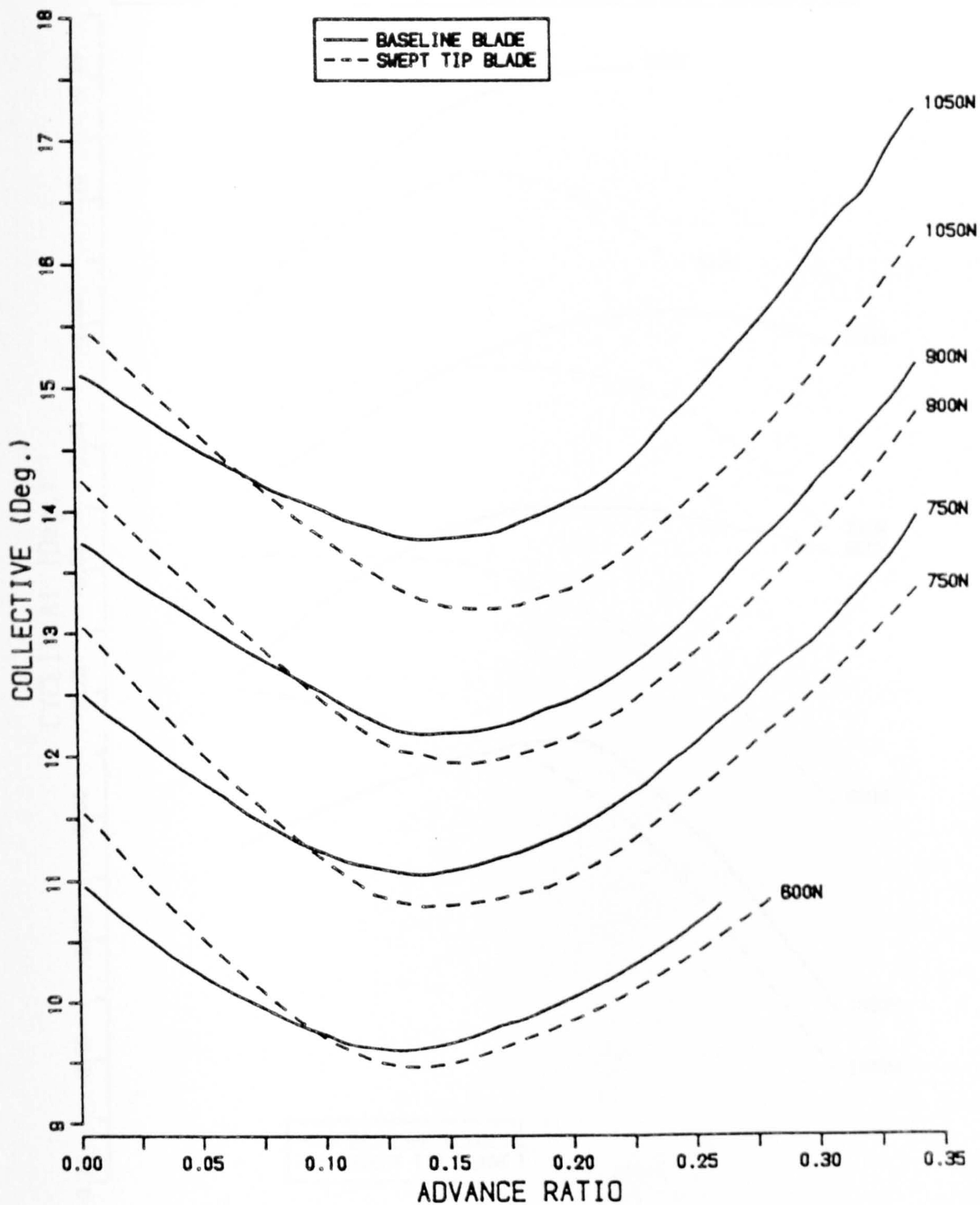


Fig 46 Measured collective pitch on the Mk2 swept tip blade and rectangular blade

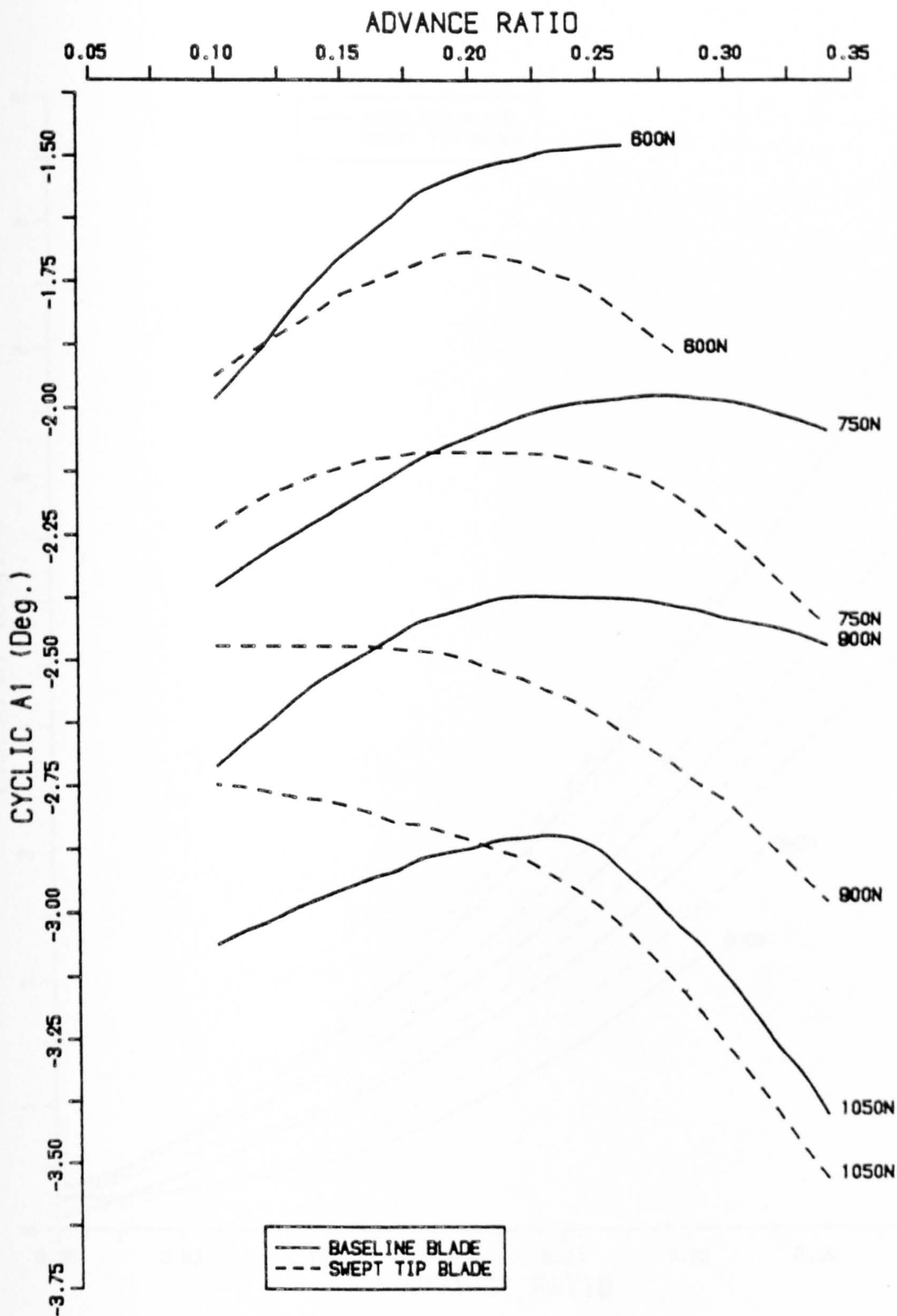


Fig 47 Measured cyclic pitch (A1) on the Mk2 swept tip blade and rectangular blade

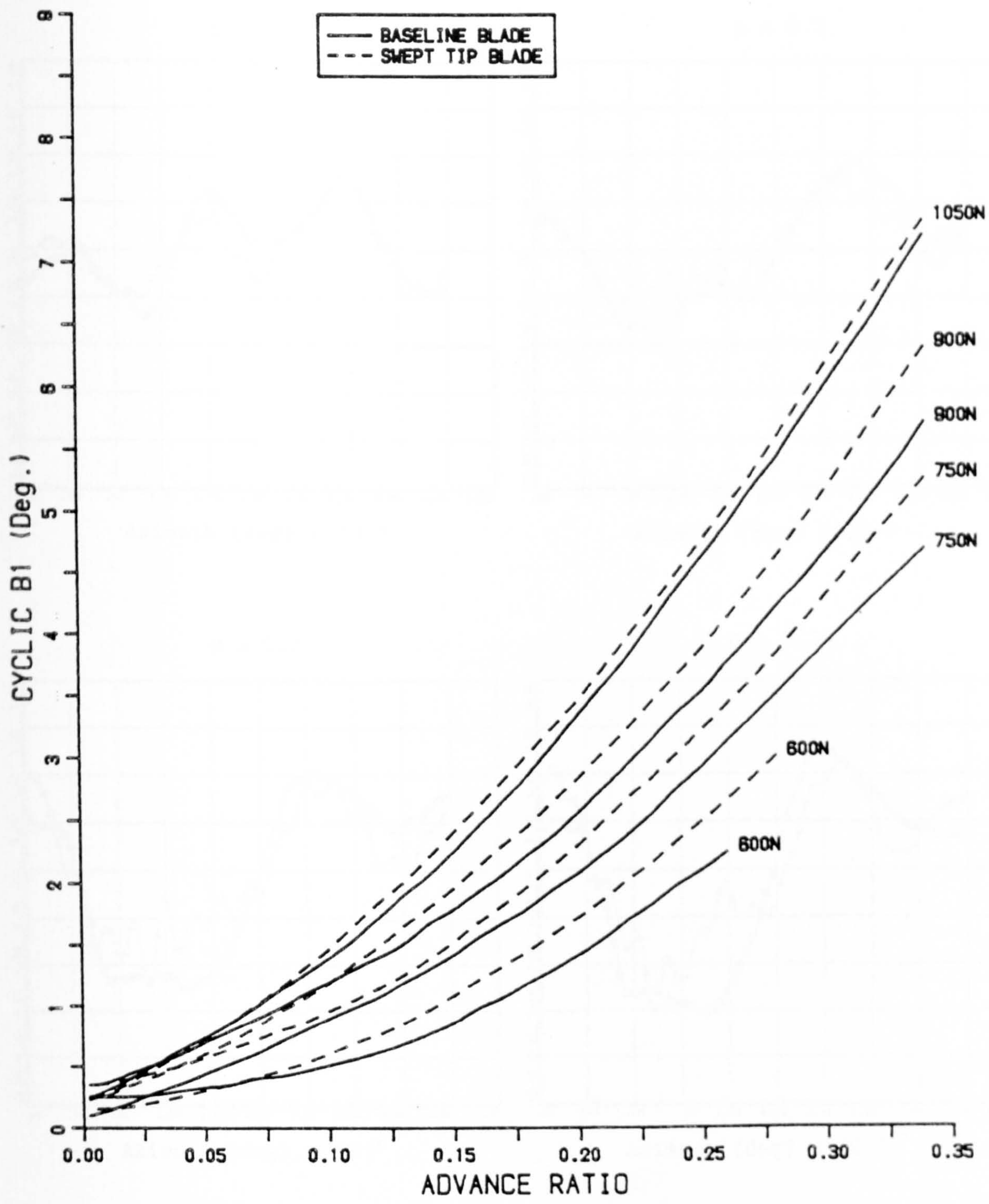


Fig 48 Measured cyclic pitch (B1) on the Mk2 swept tip blade and rectangular blade

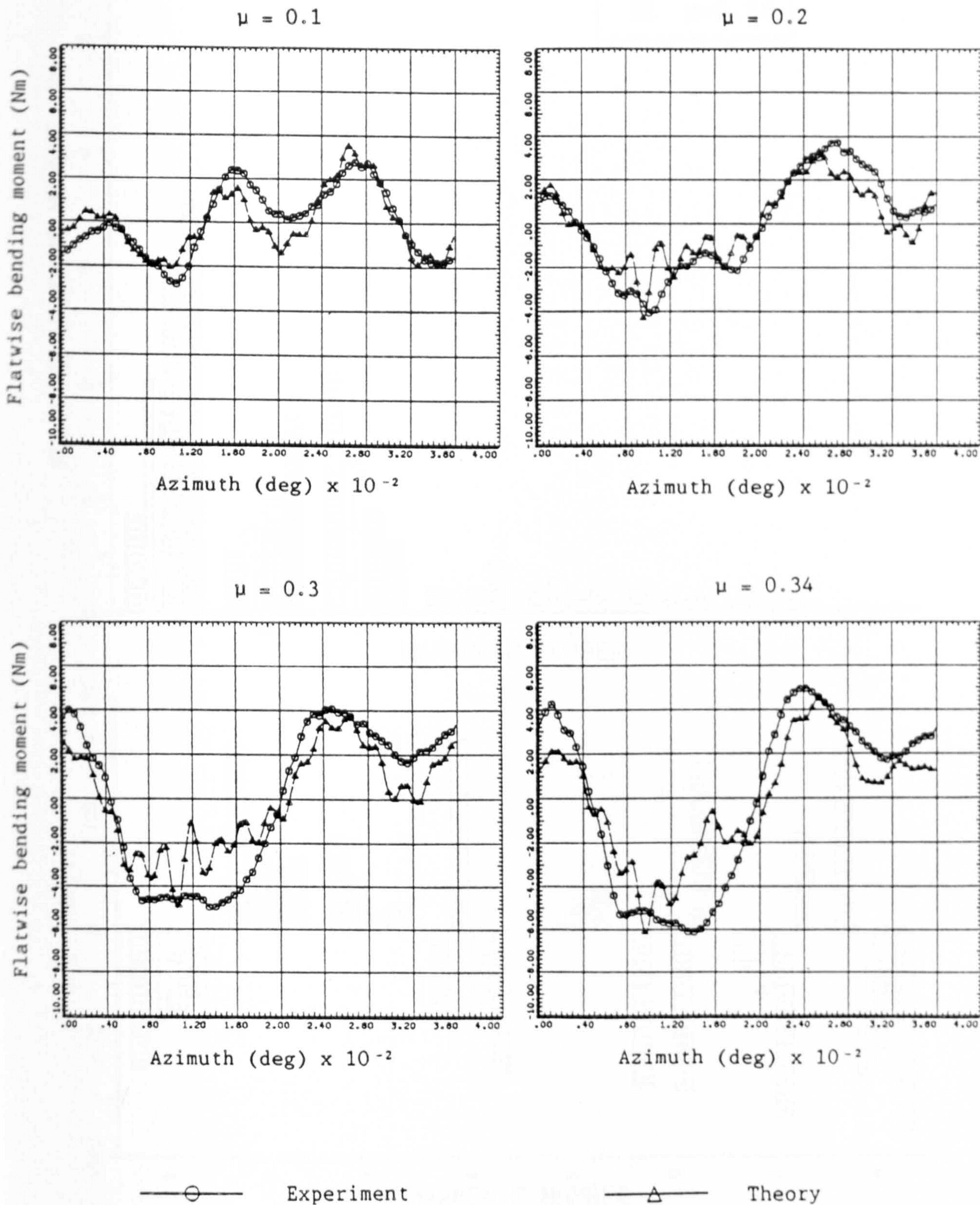


Fig 49 Measured and predicted (J134) flatwise bending moments for various advance ratios, thrust = 900N, 37% R

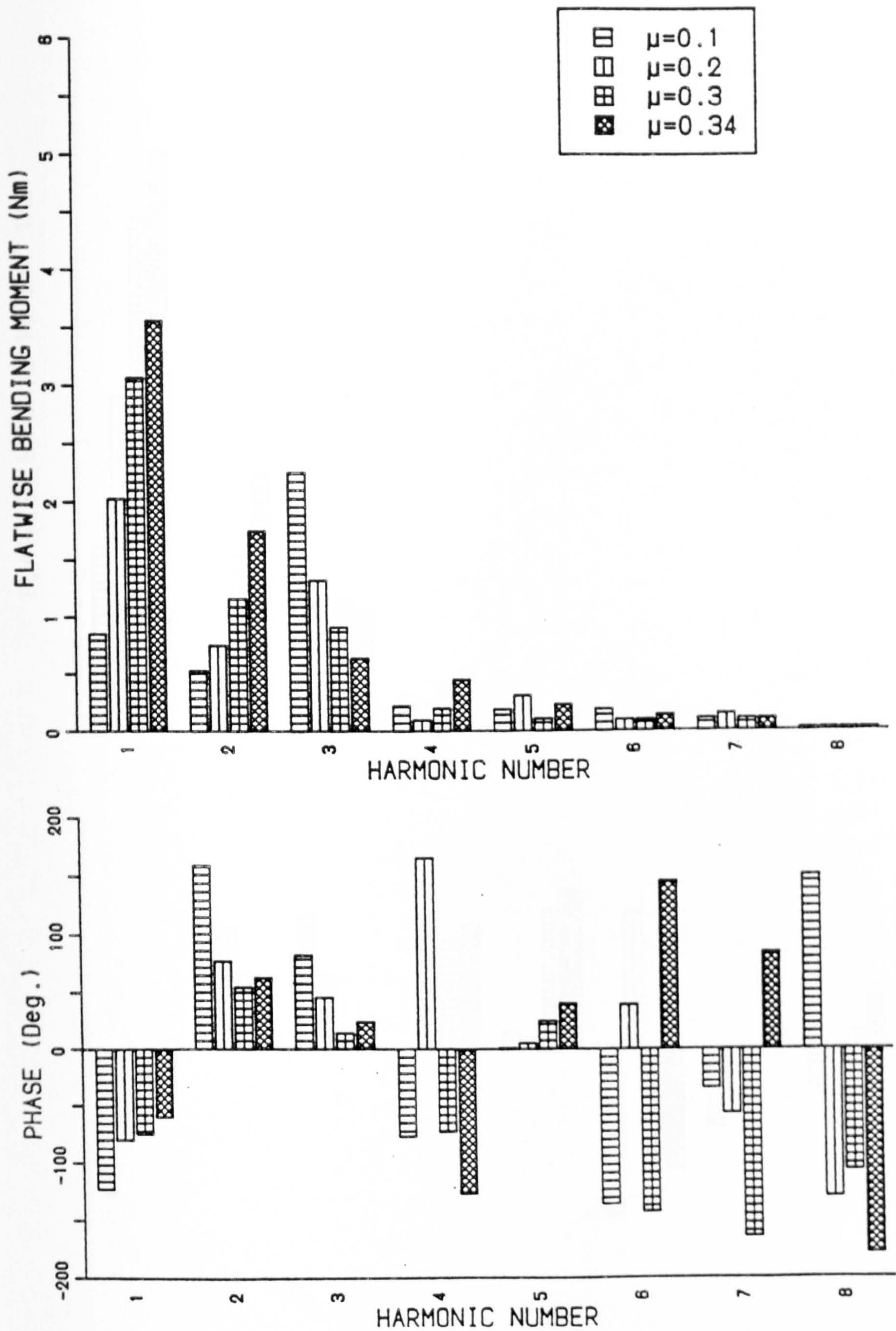


Fig 50 Harmonic analysis of the predicted (J134) flatwise bending moments on the Mk2 swept tip blade, thrust = 900N, 37% R

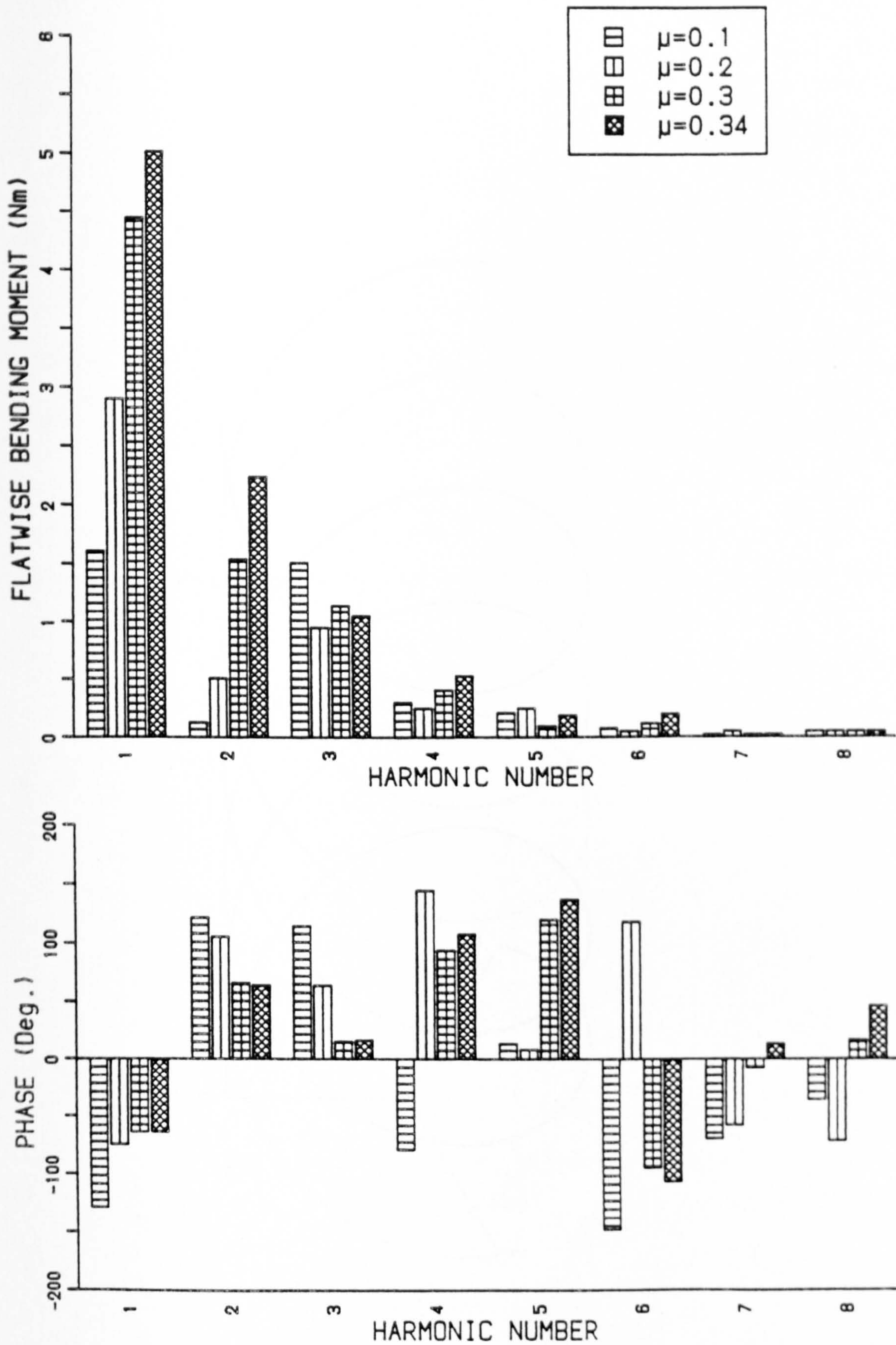


Fig 51 Harmonic analysis of the measured flatwise bending moments on the Mk2 swept tip blade, thrust = 900N, 37% R

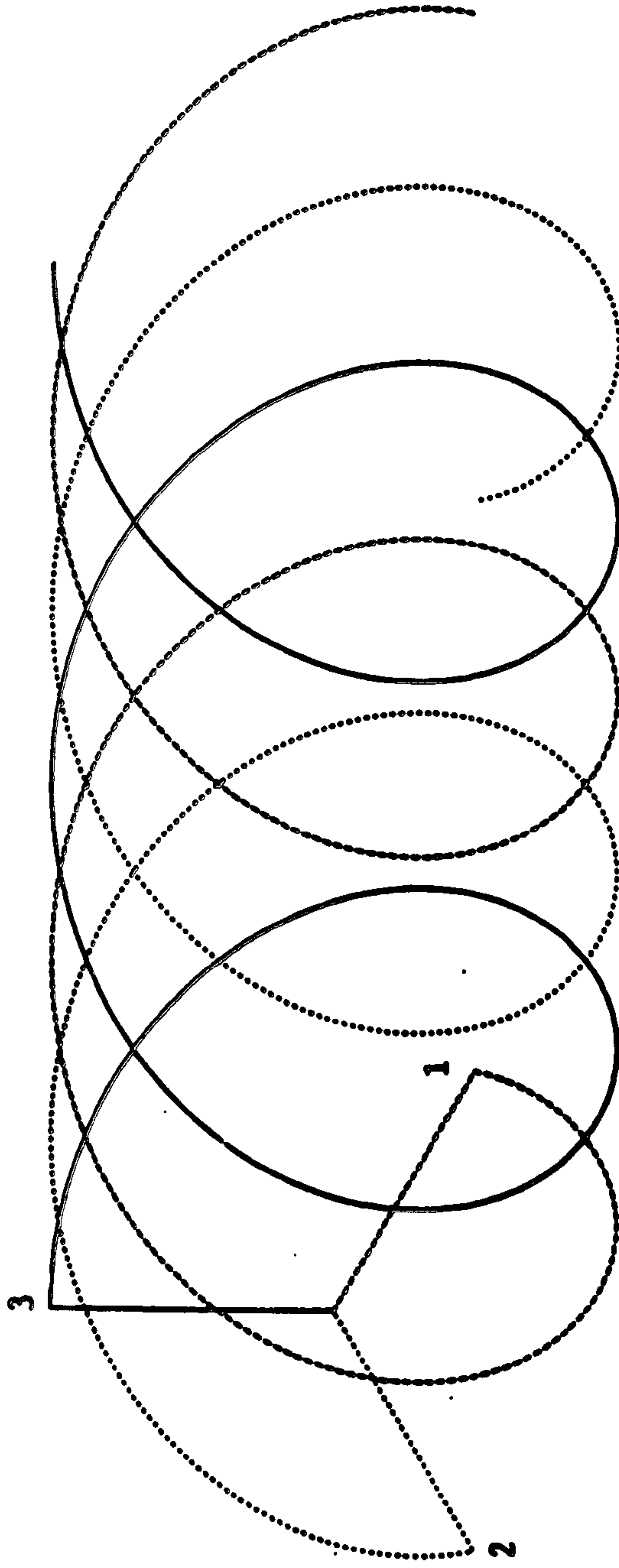
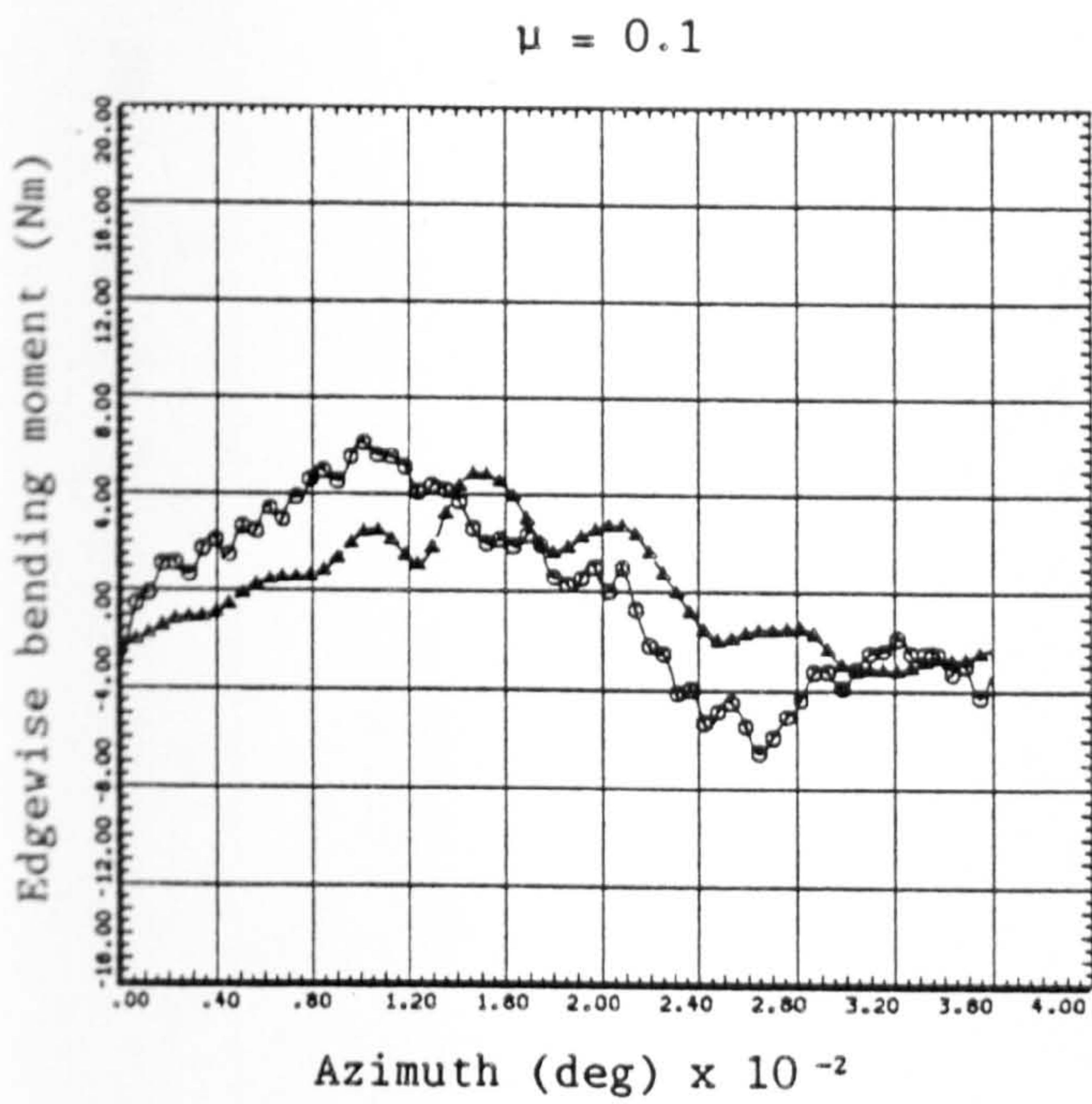
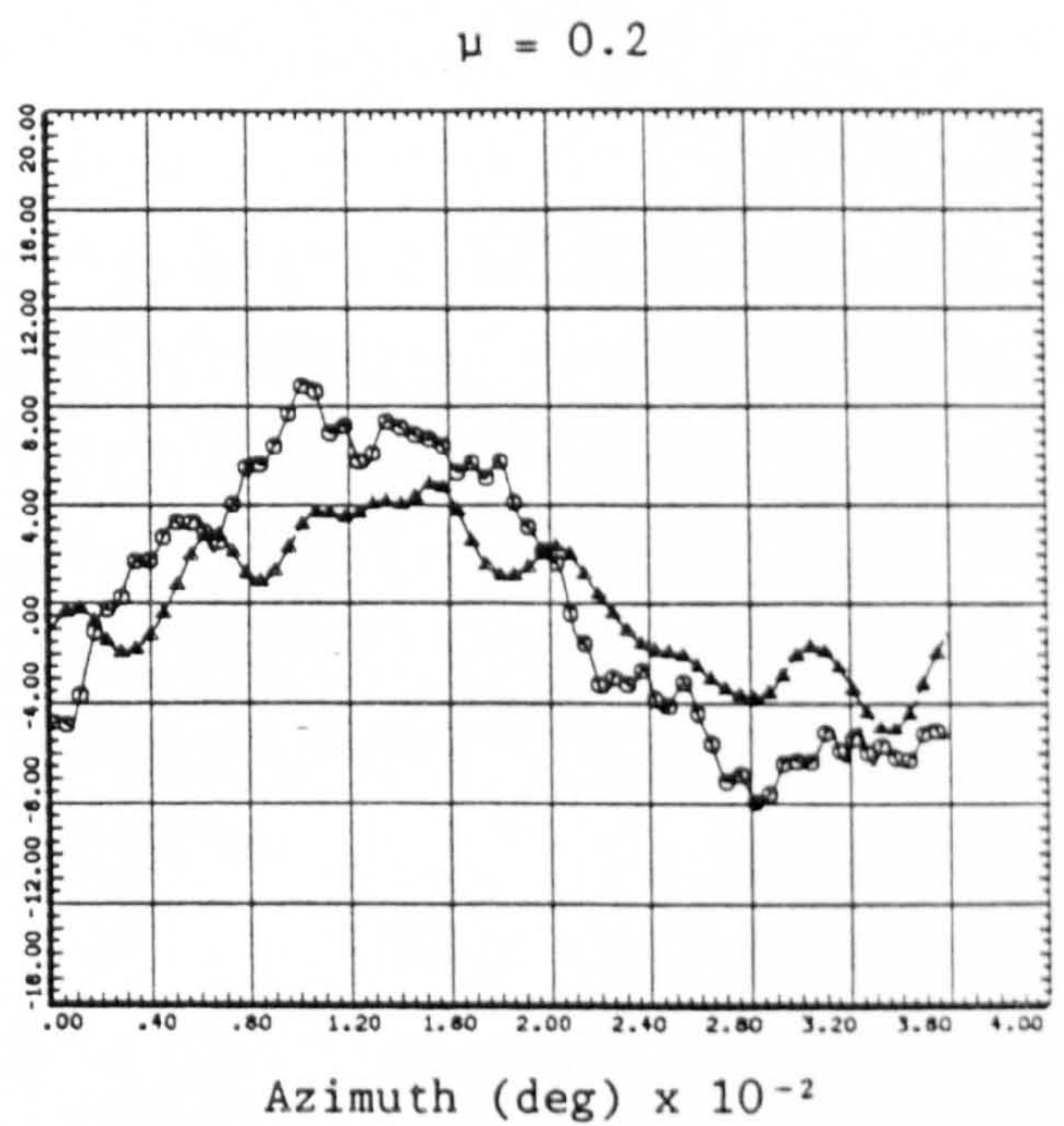
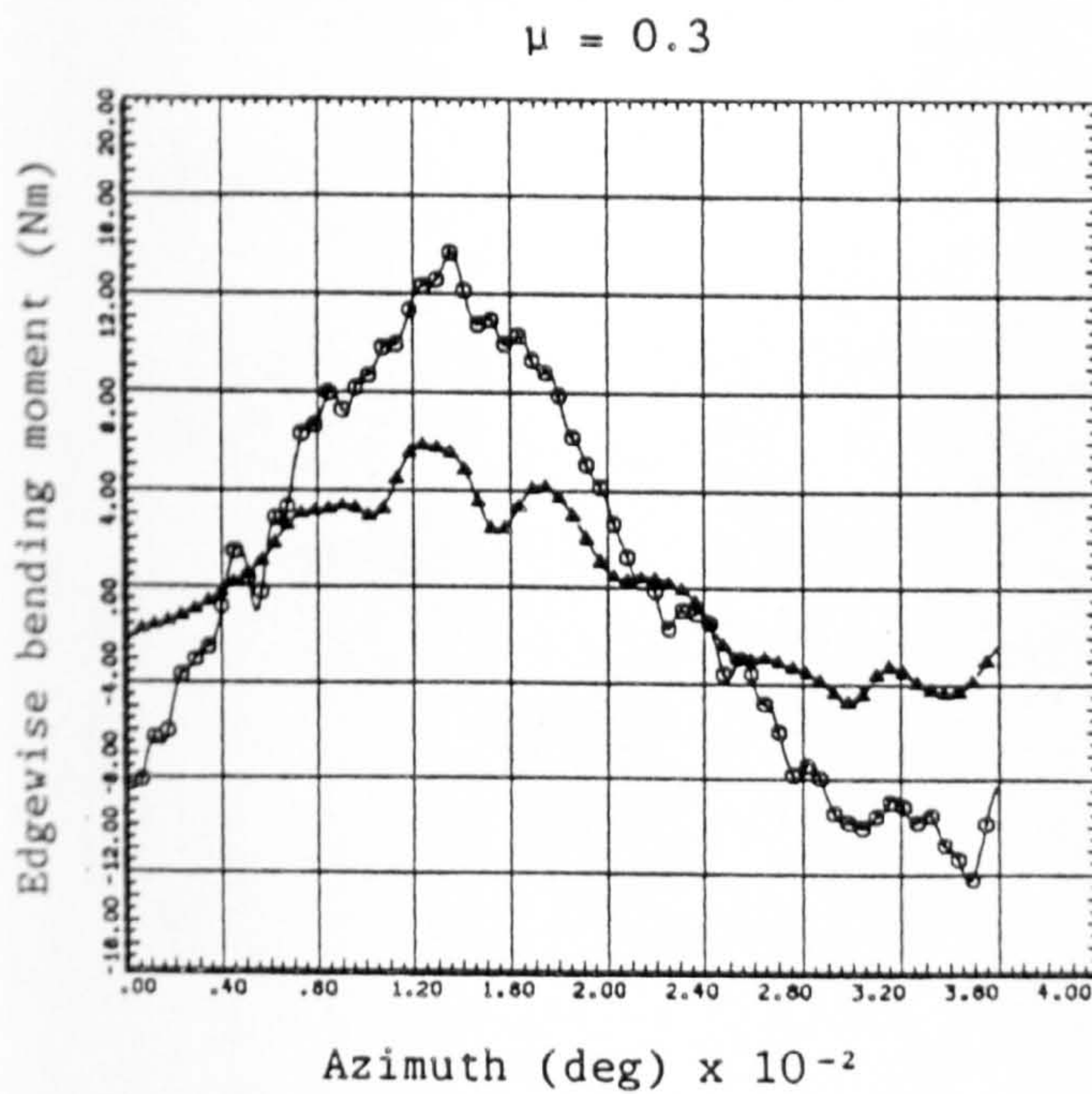
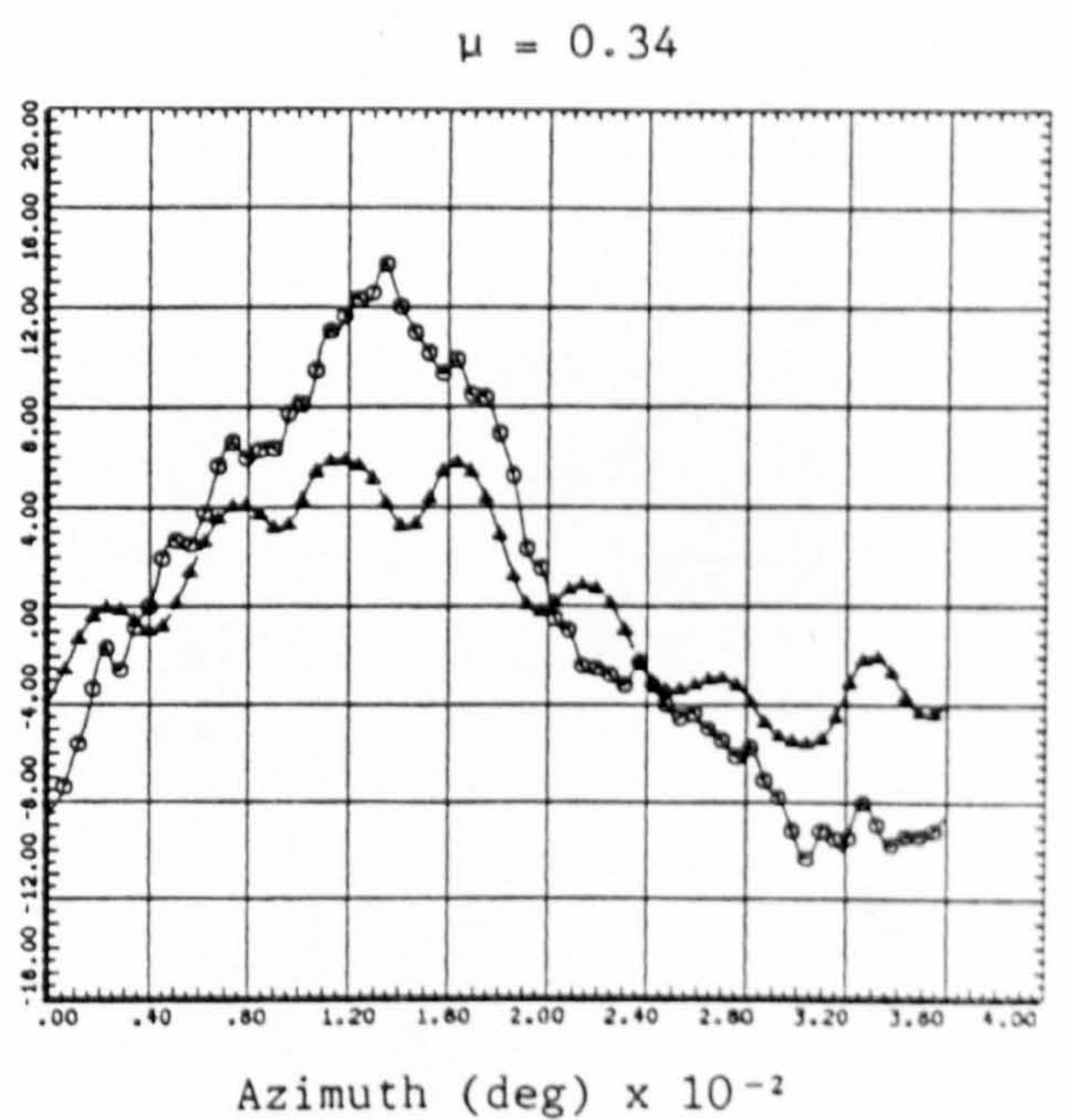


Fig 52 Tip vortex path at $\mu = 0.3$



—○— Experiment

—△— Theory

Fig 53 Measured and predicted (J134) edgewise bending moments for various advance ratios, thrust = 900N, 37% R

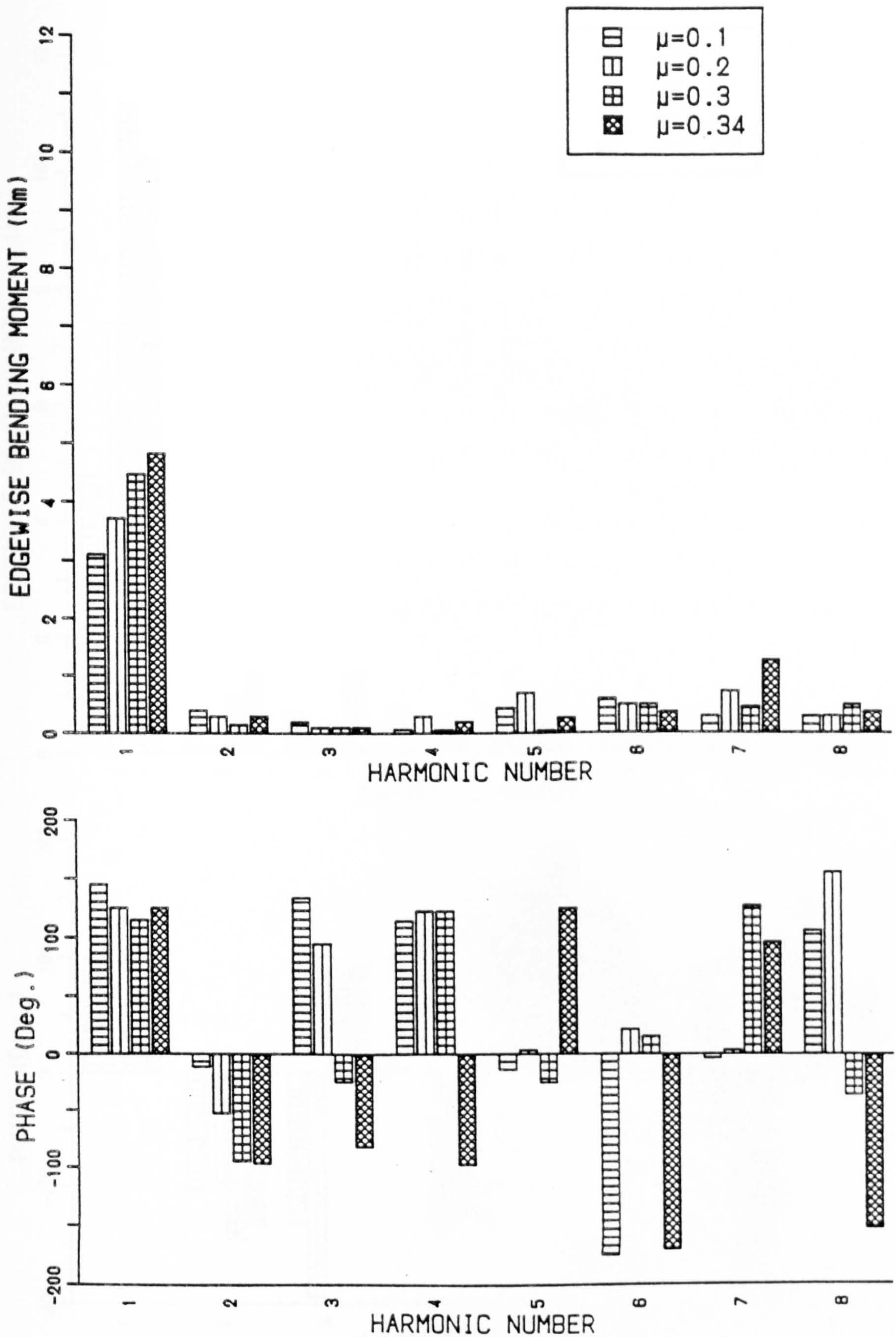


Fig 54 Harmonic analysis of the predicted (J134) edgewise bending moments on the Mk2 swept tip blade, thrust = 900N, 37% R

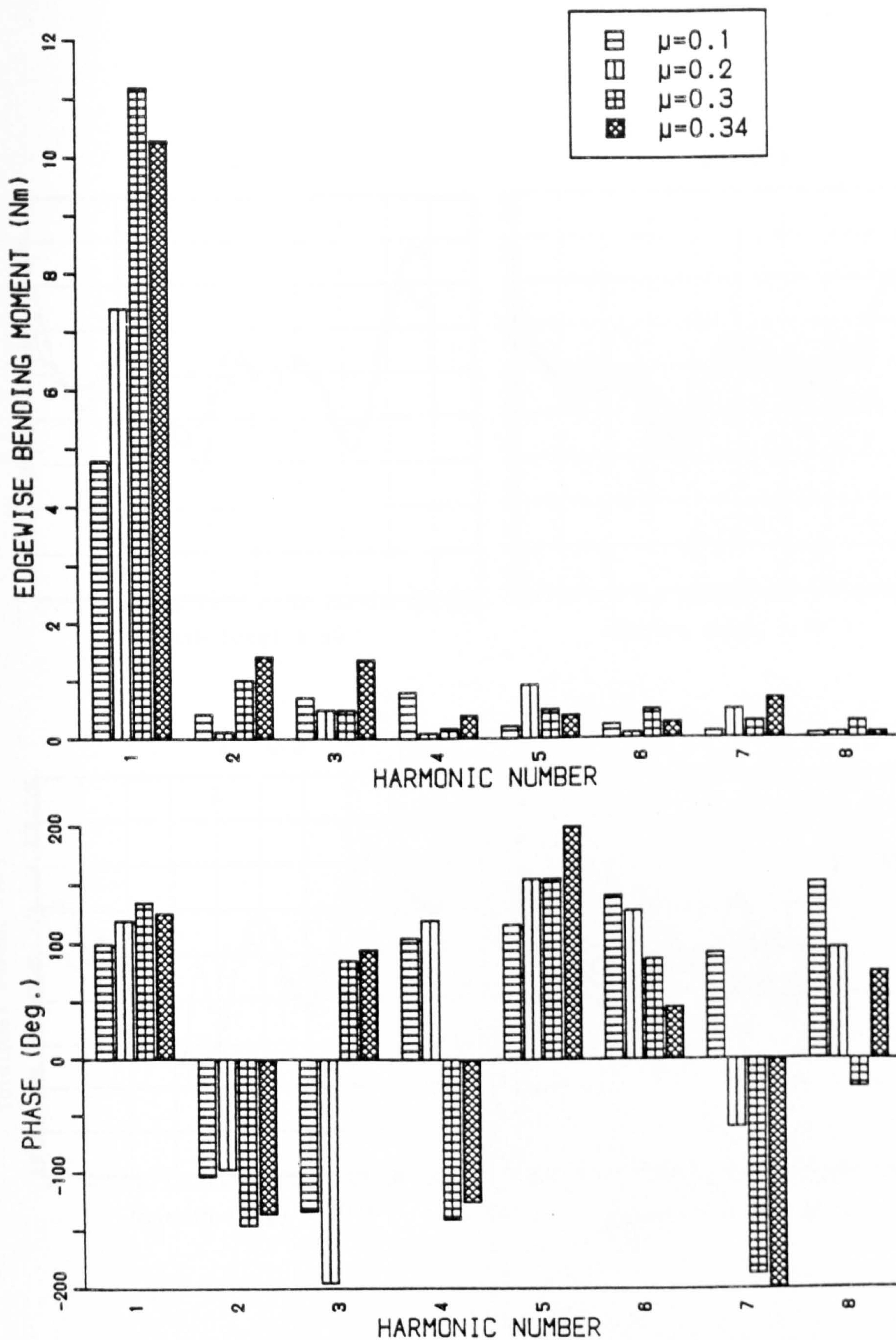
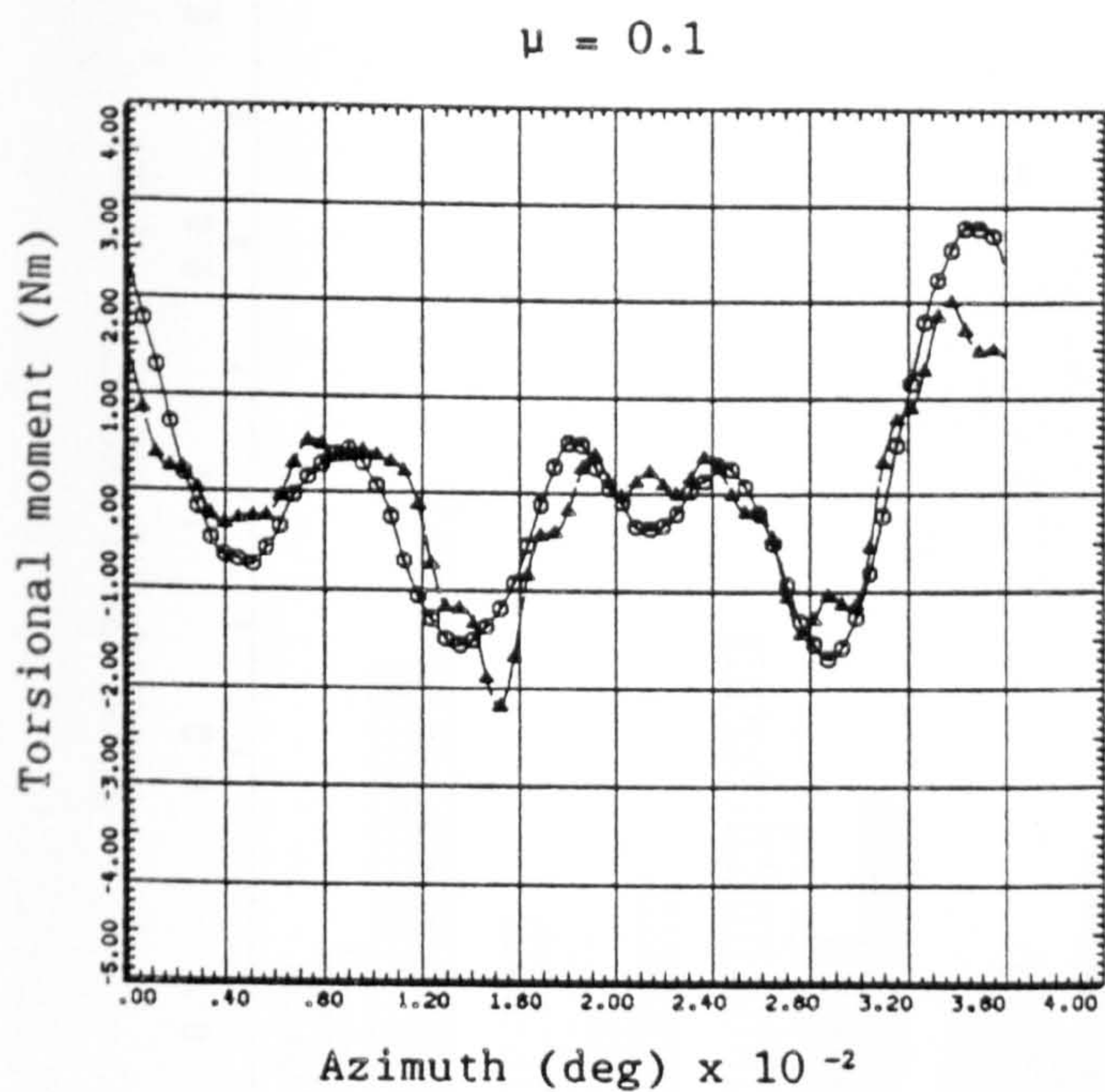
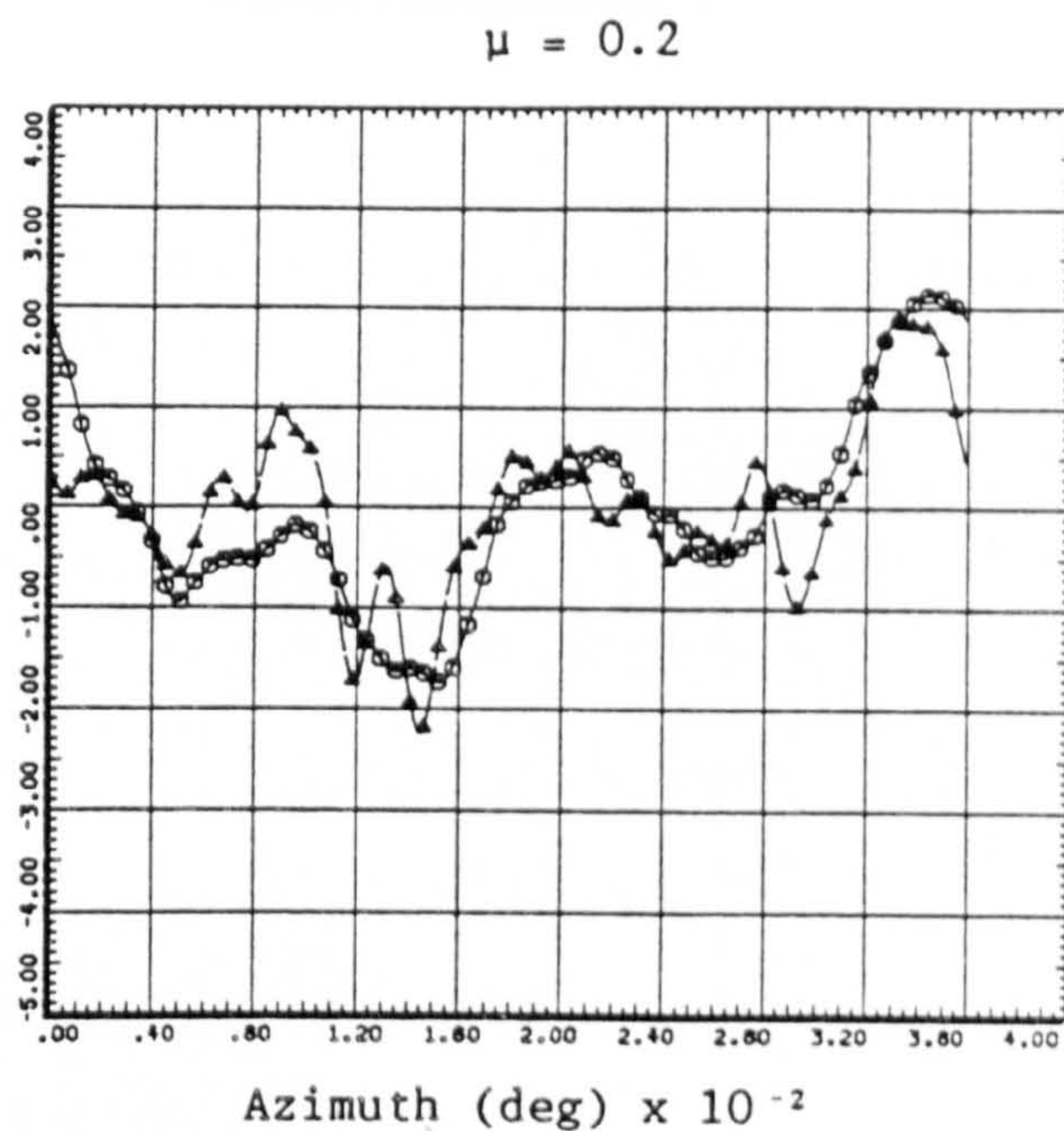
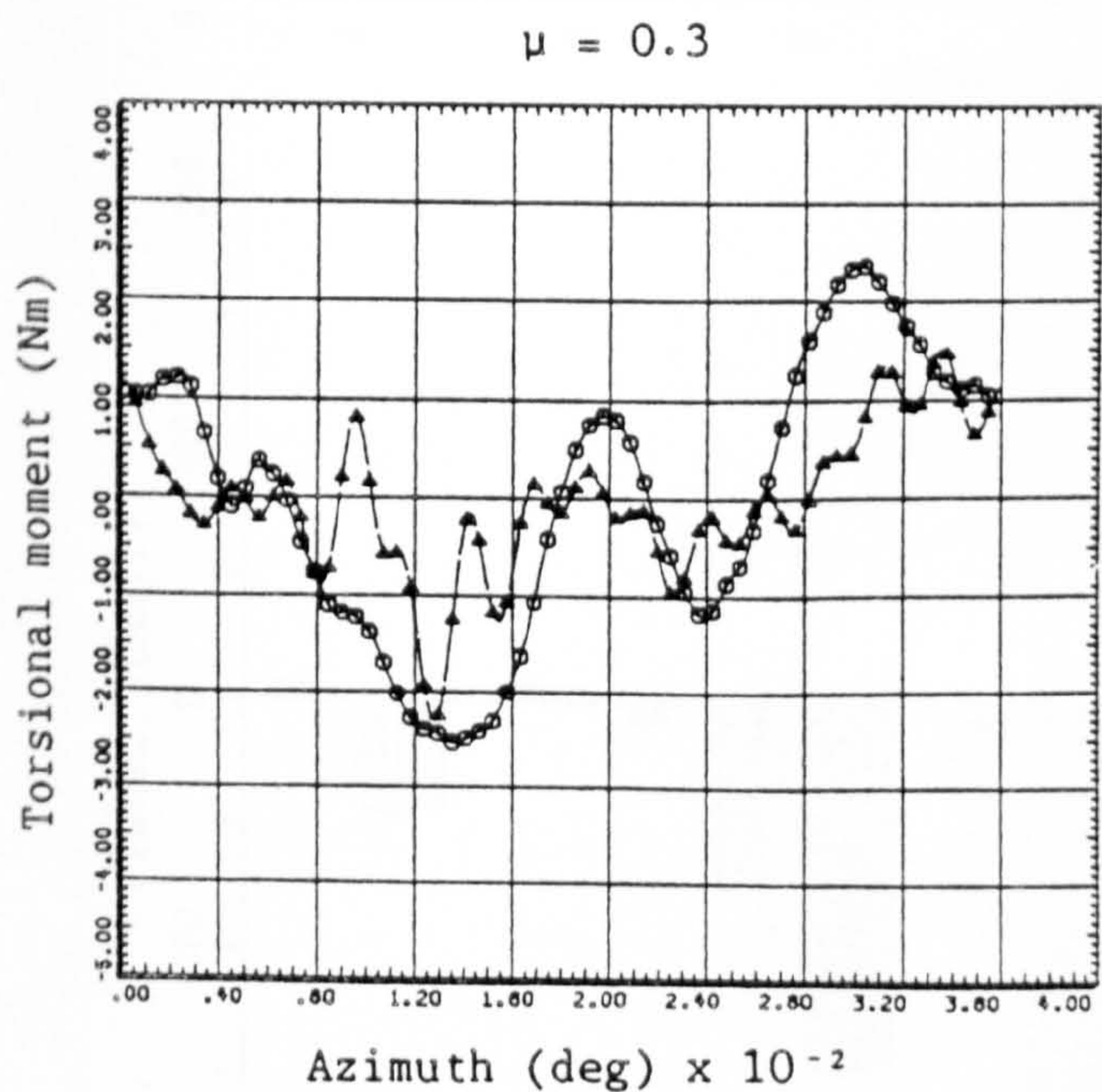
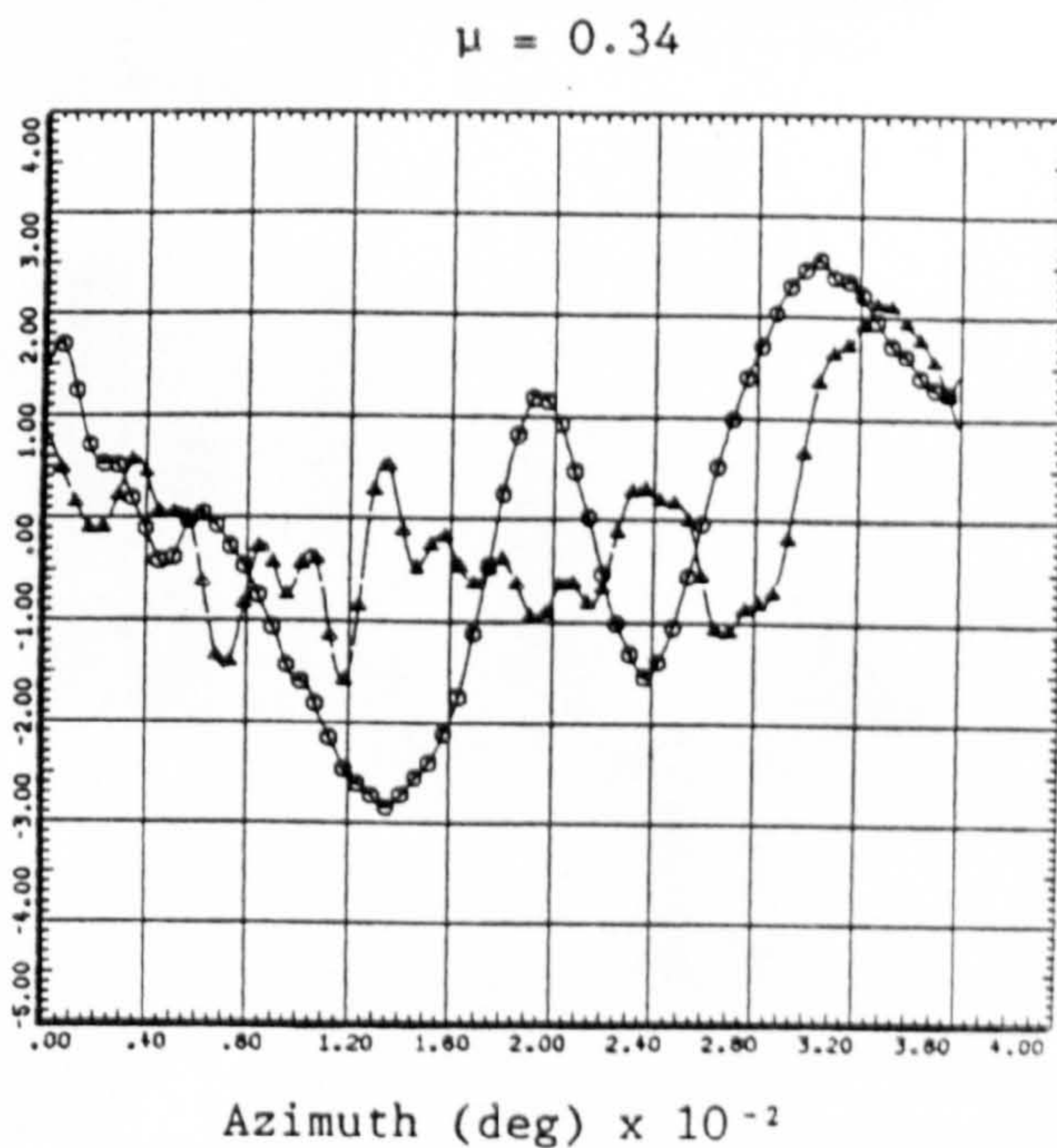


Fig 55 Harmonic analysis of the measured edgewise bending moments on the Mk2 swept tip blade, thrust = 900N, 37% R



—○— Experiment

—△— Theory

Fig 56 Measured and predicted (J134) torsional moments for various advance ratios, thrust = 900N, 37% R

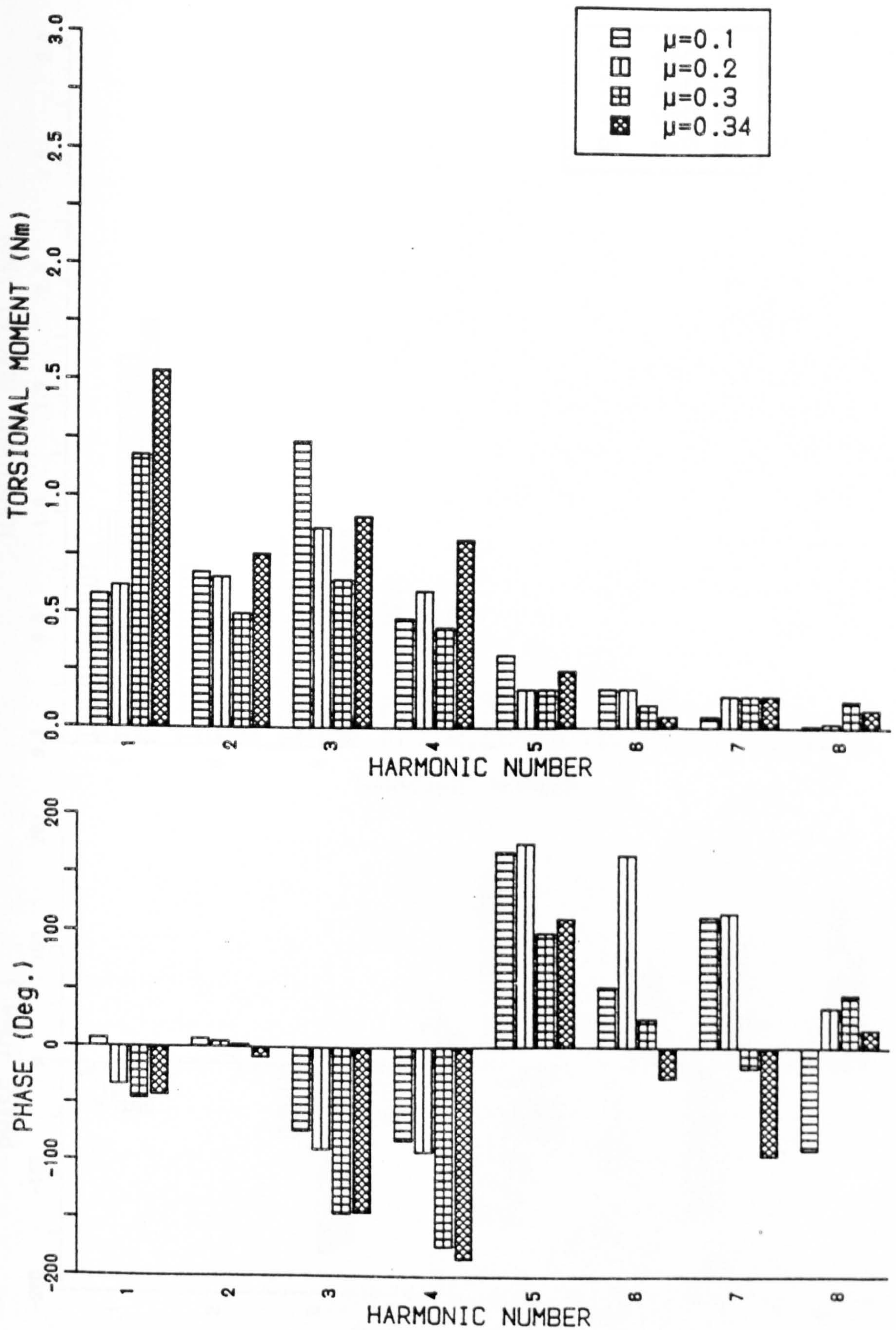


Fig 57 Harmonic analysis of the predicted (J134) torsional moments on the Mk2 swept tip blade, thrust = 900N, 37% R

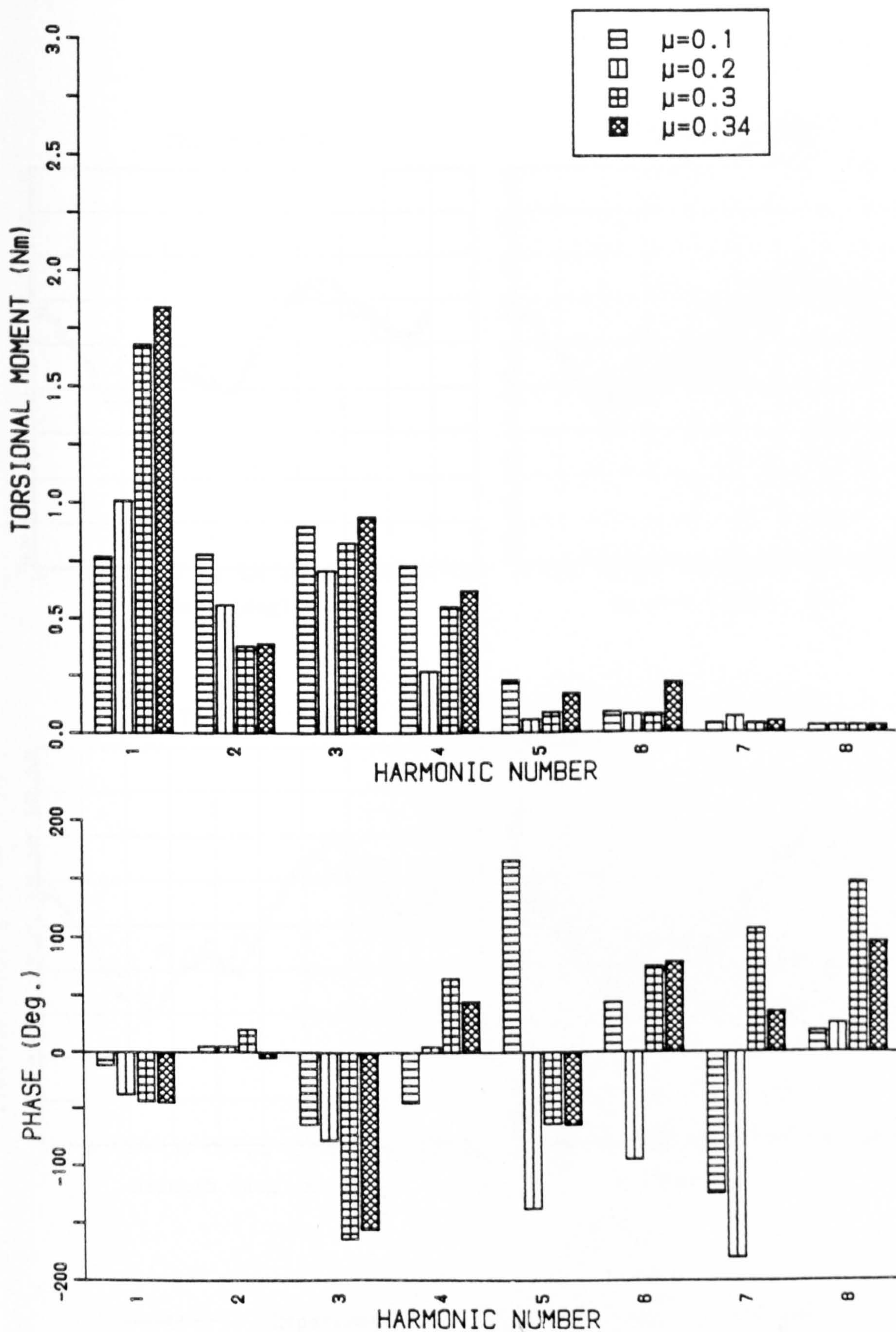
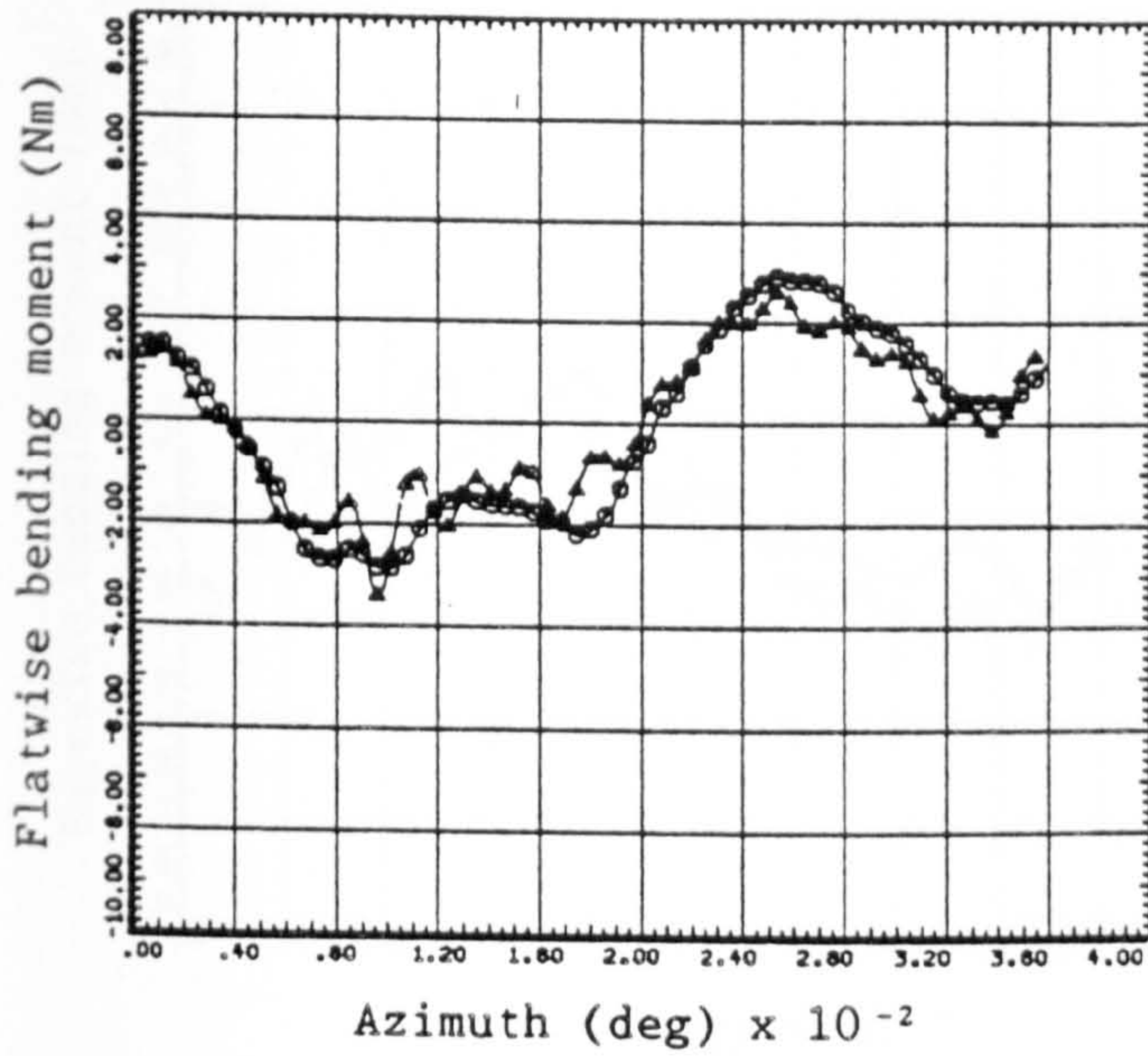
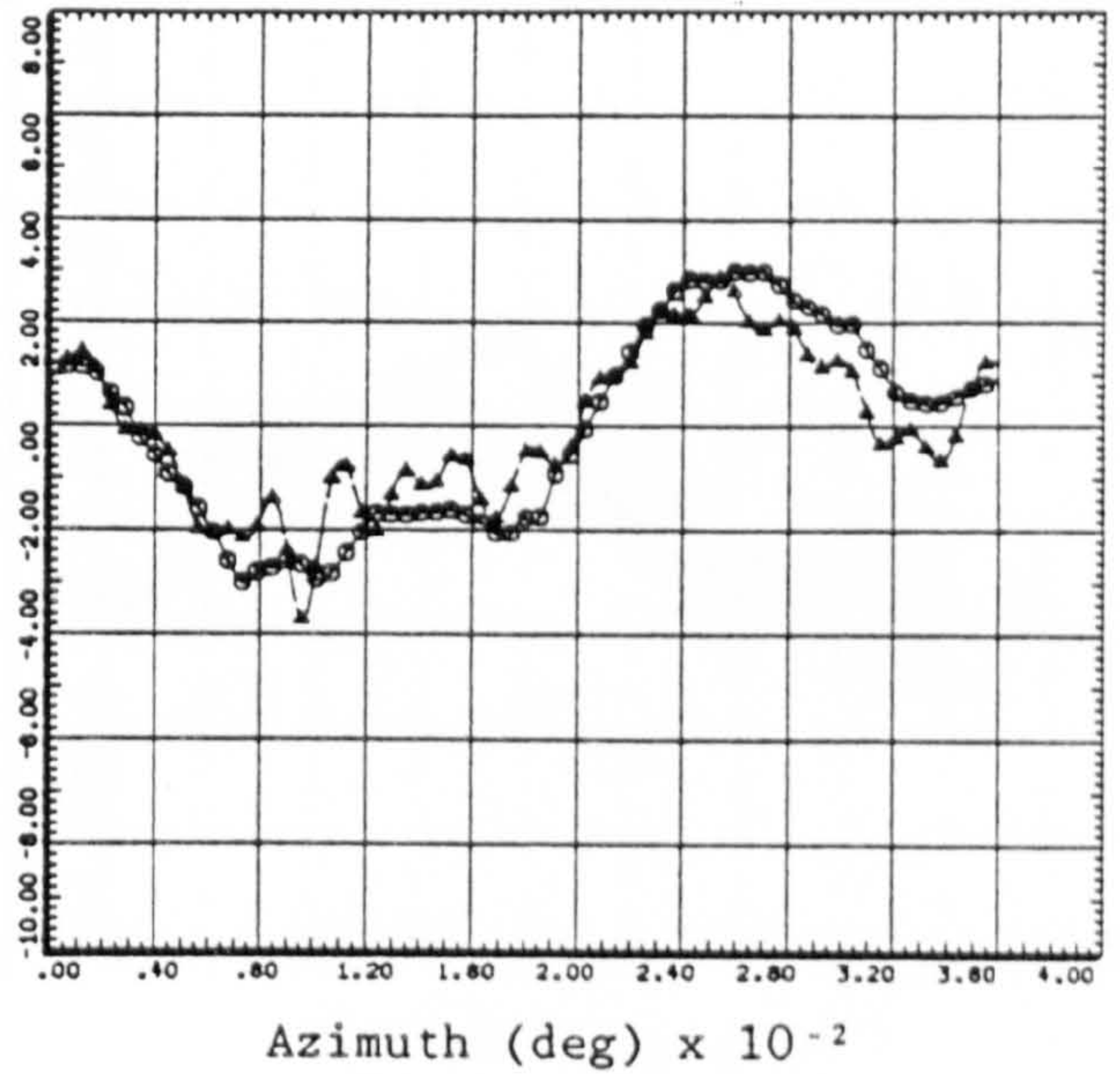


Fig 58 Harmonic analysis of the measured torsional moments on the Mk2 swept tip blade, thrust = 900N, 37% R

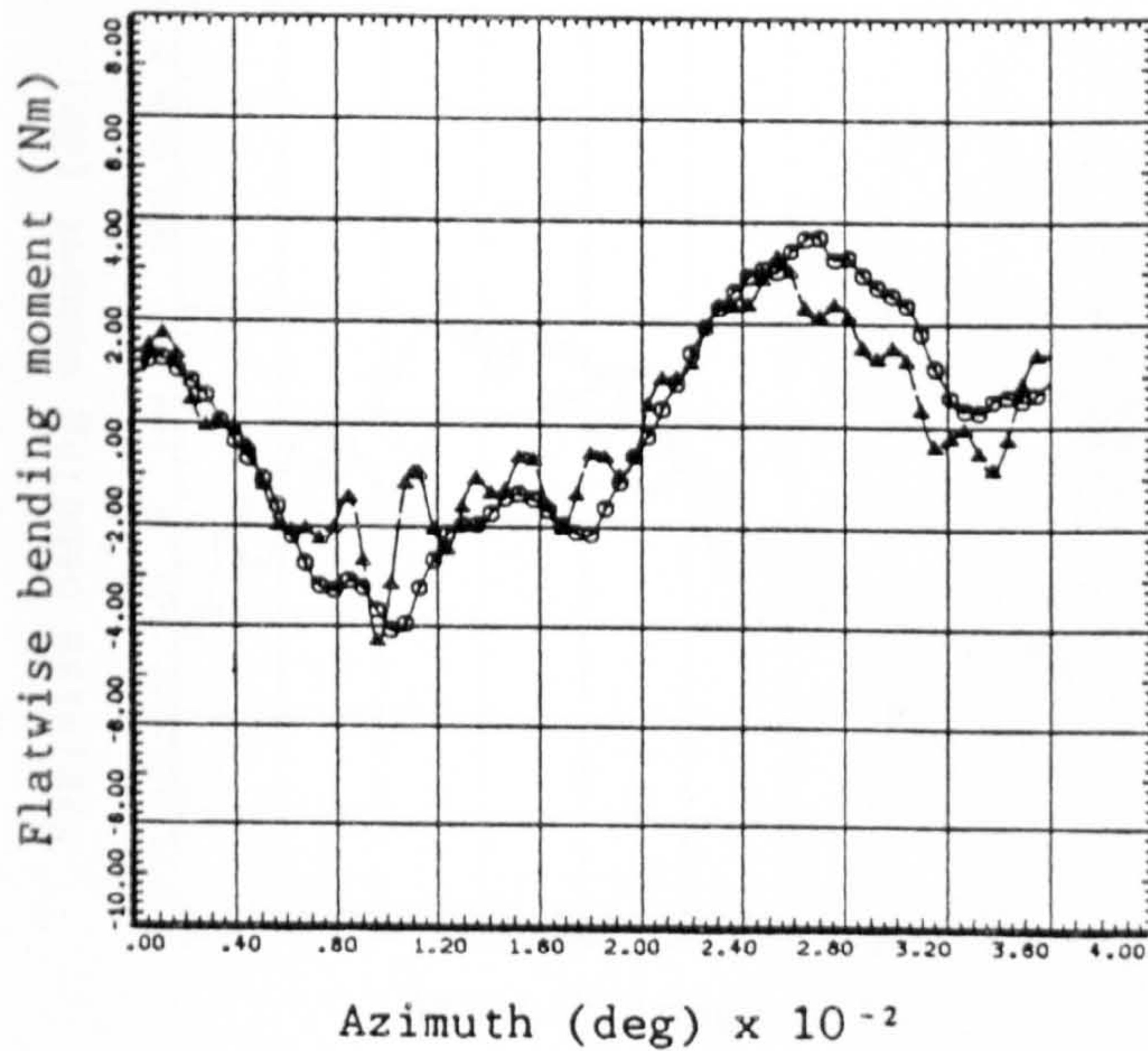
Thrust = 600N



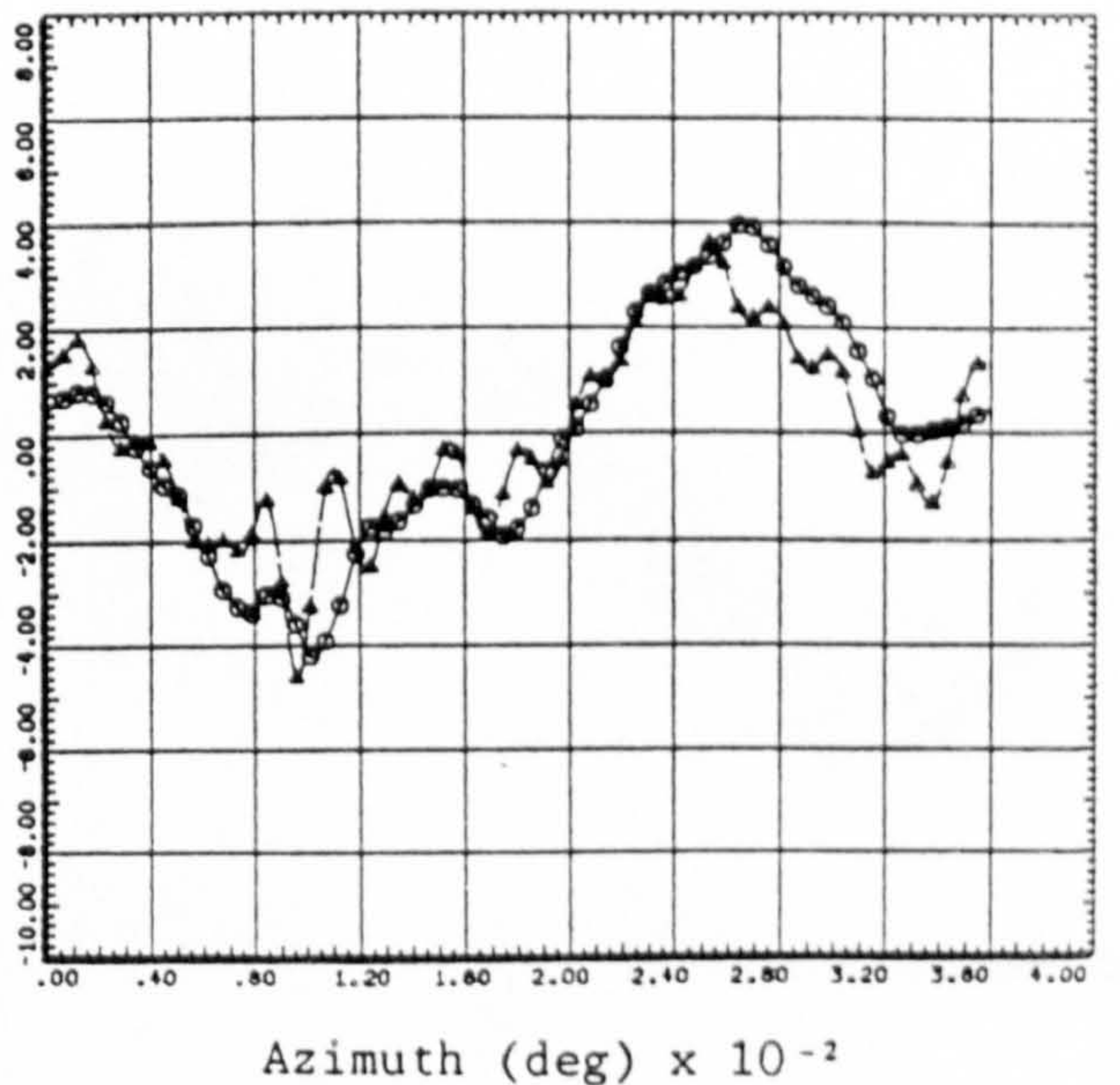
Thrust = 750N



Thrust = 900N



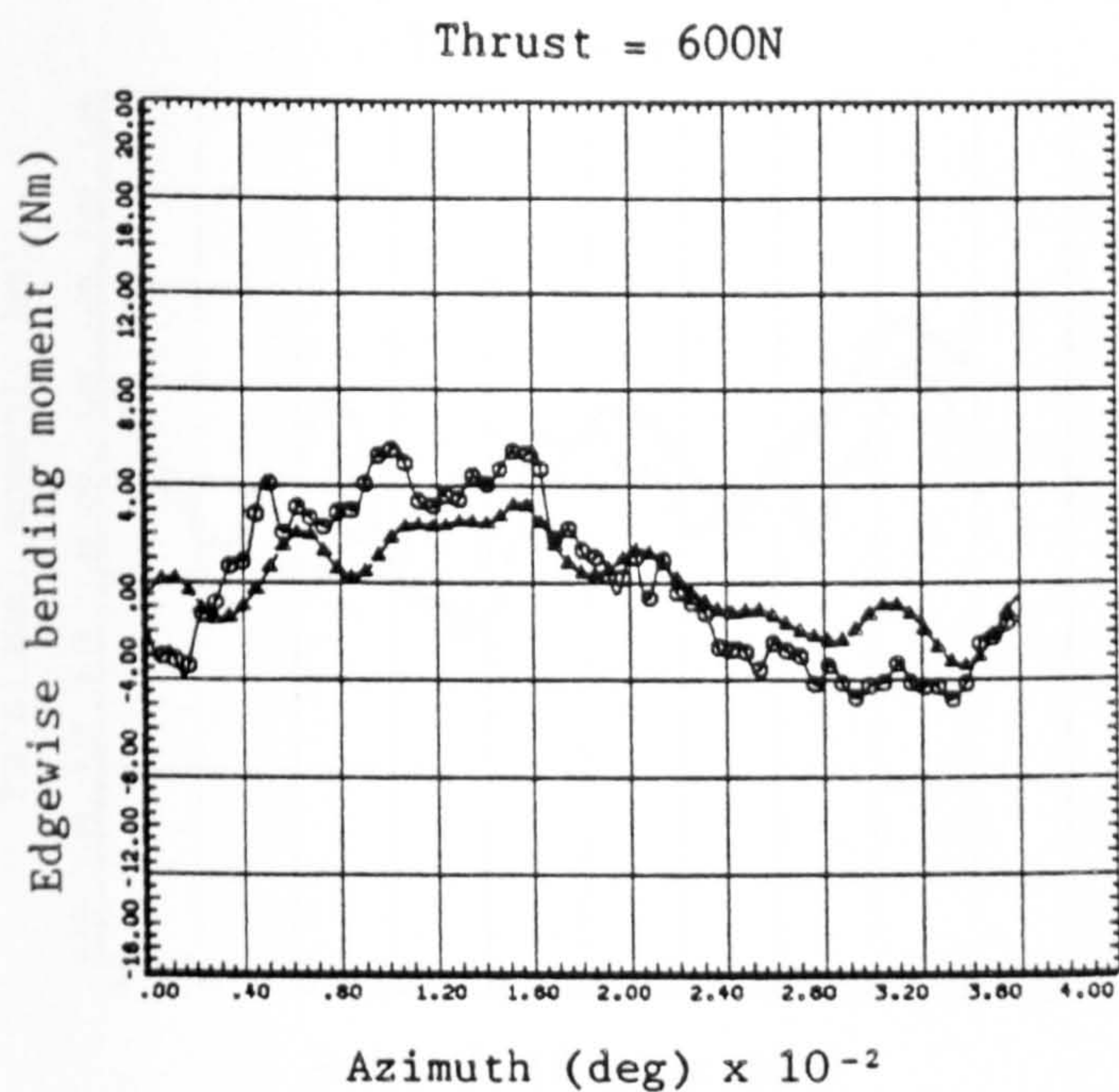
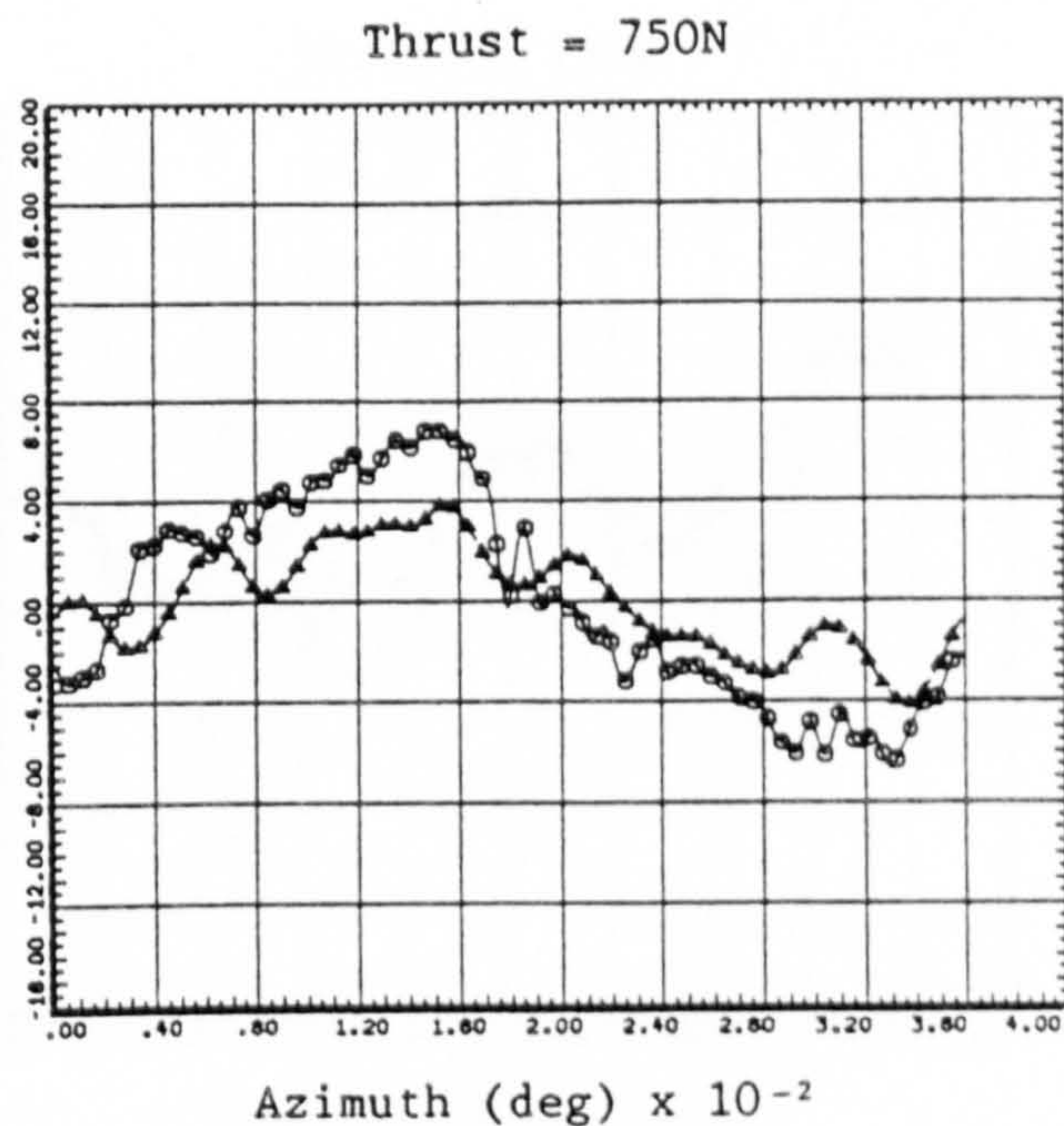
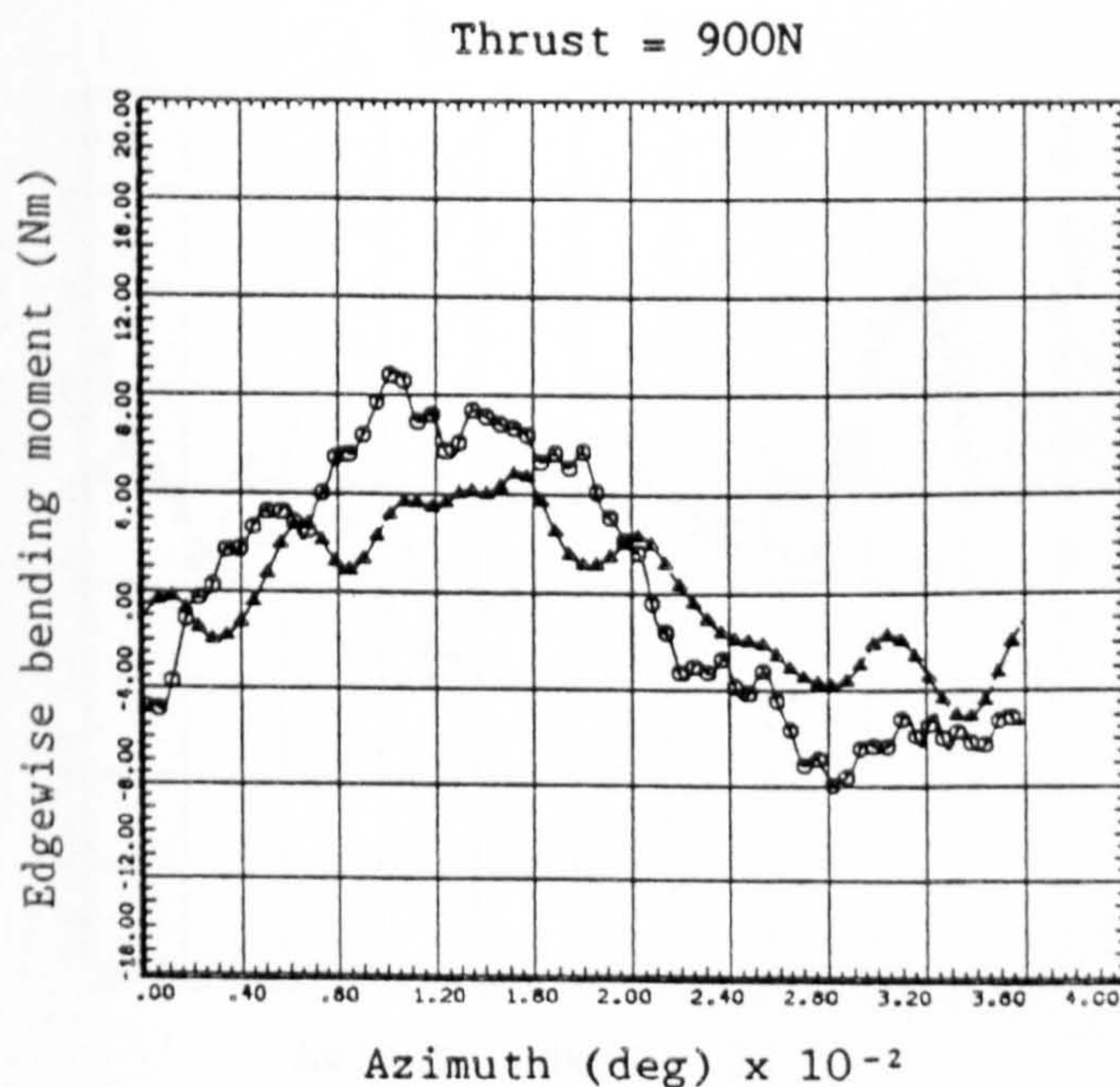
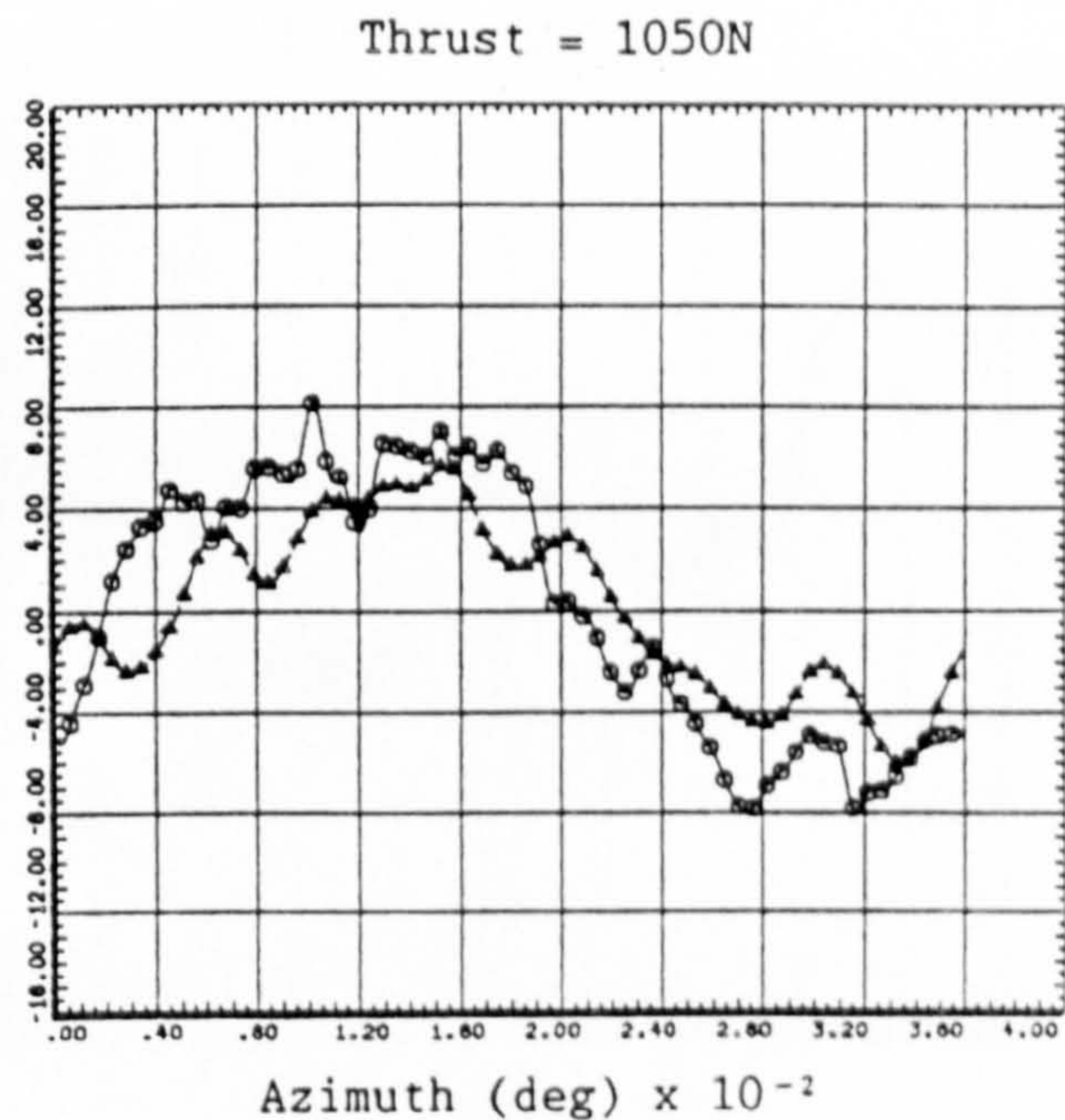
Thrust = 1050N



—○— Experiment

—△— Theory

Fig 59 Measured and predicted (J134) flatwise bending moments for various thrusts, $\mu = 0.2$, 37% R

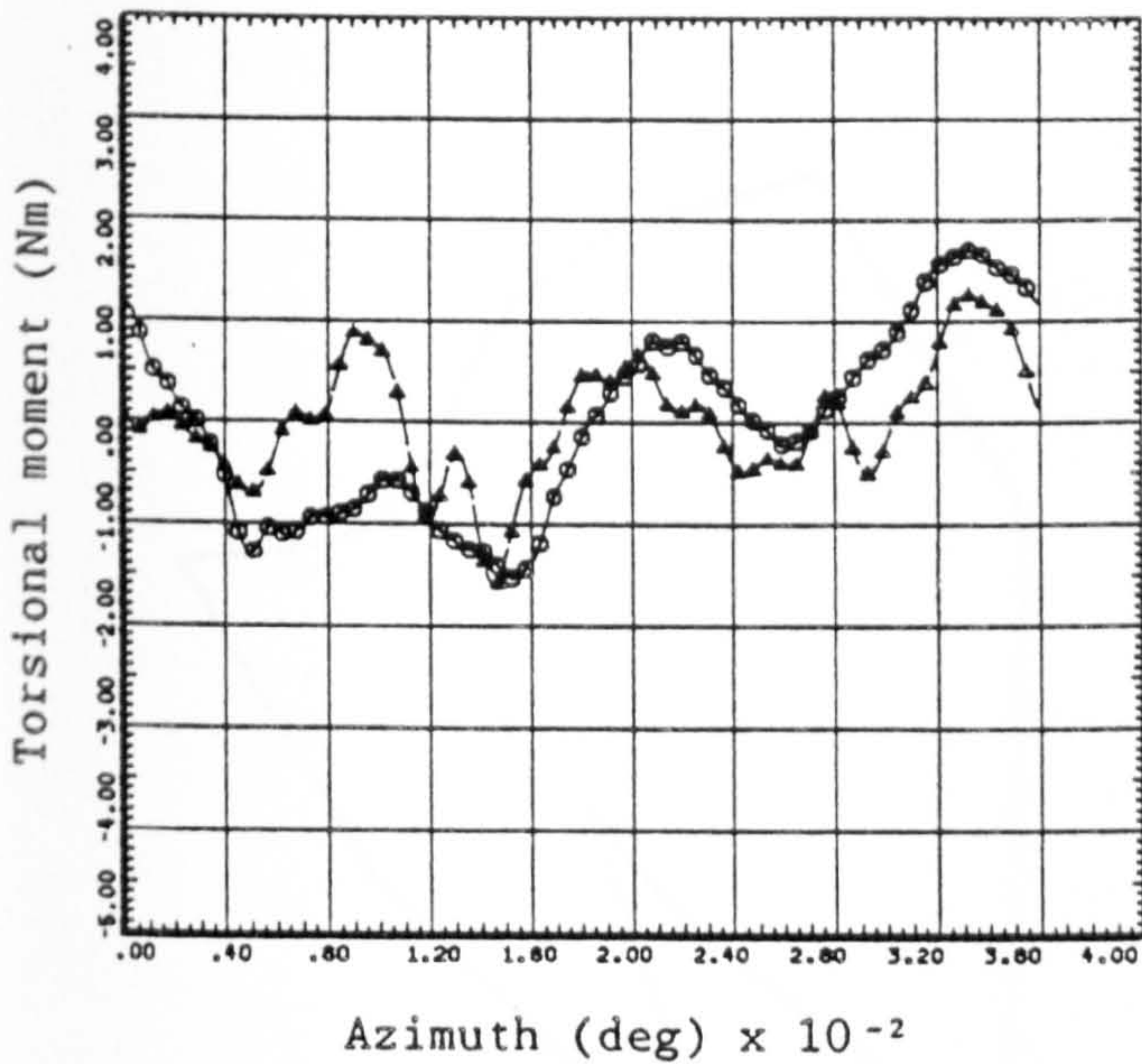


—○— Experiment

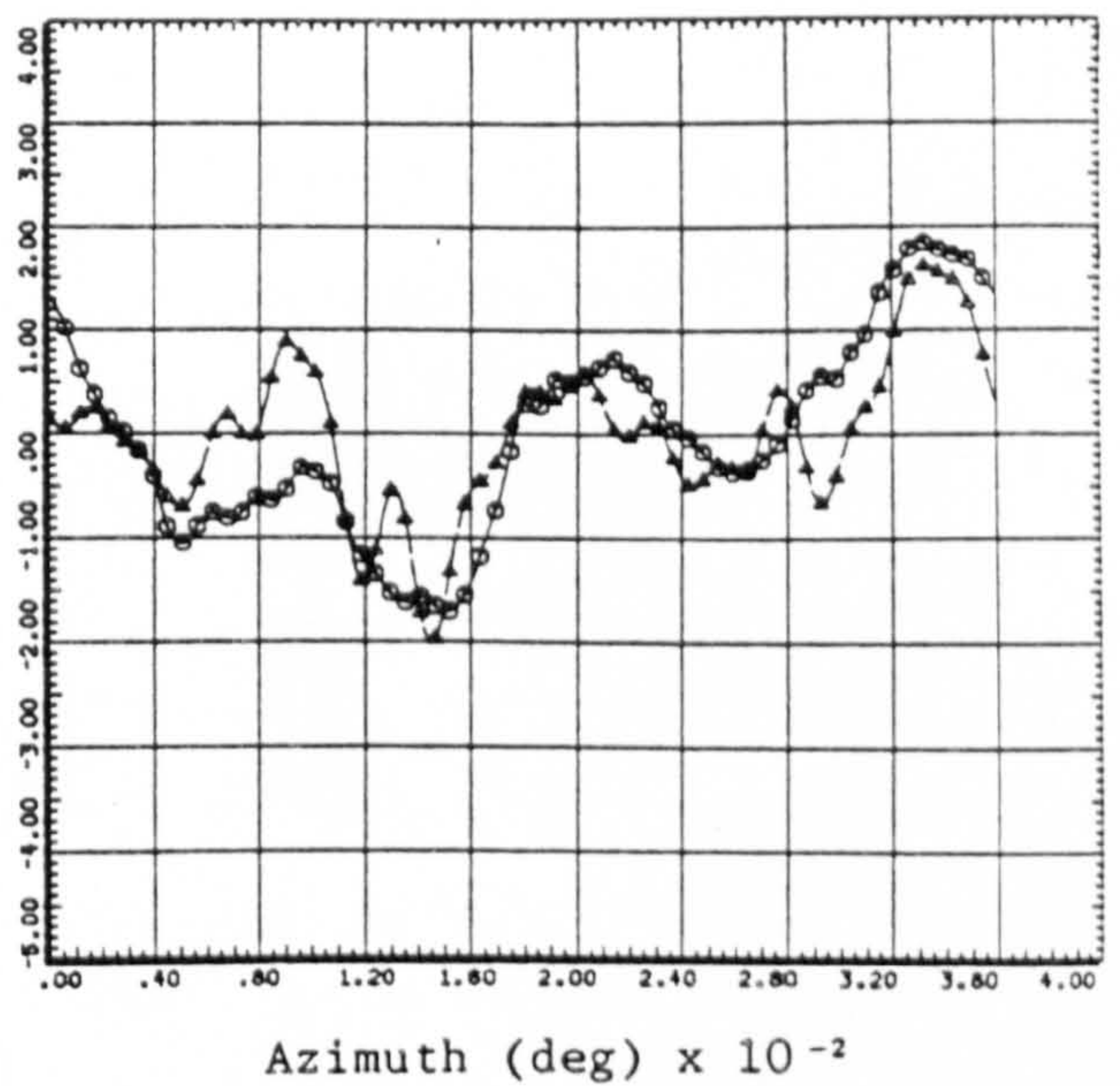
—△— Theory

Fig 60 Measured and predicted (J134) edgewise bending moments for various thrusts, $\mu = 0.2$, 37% R

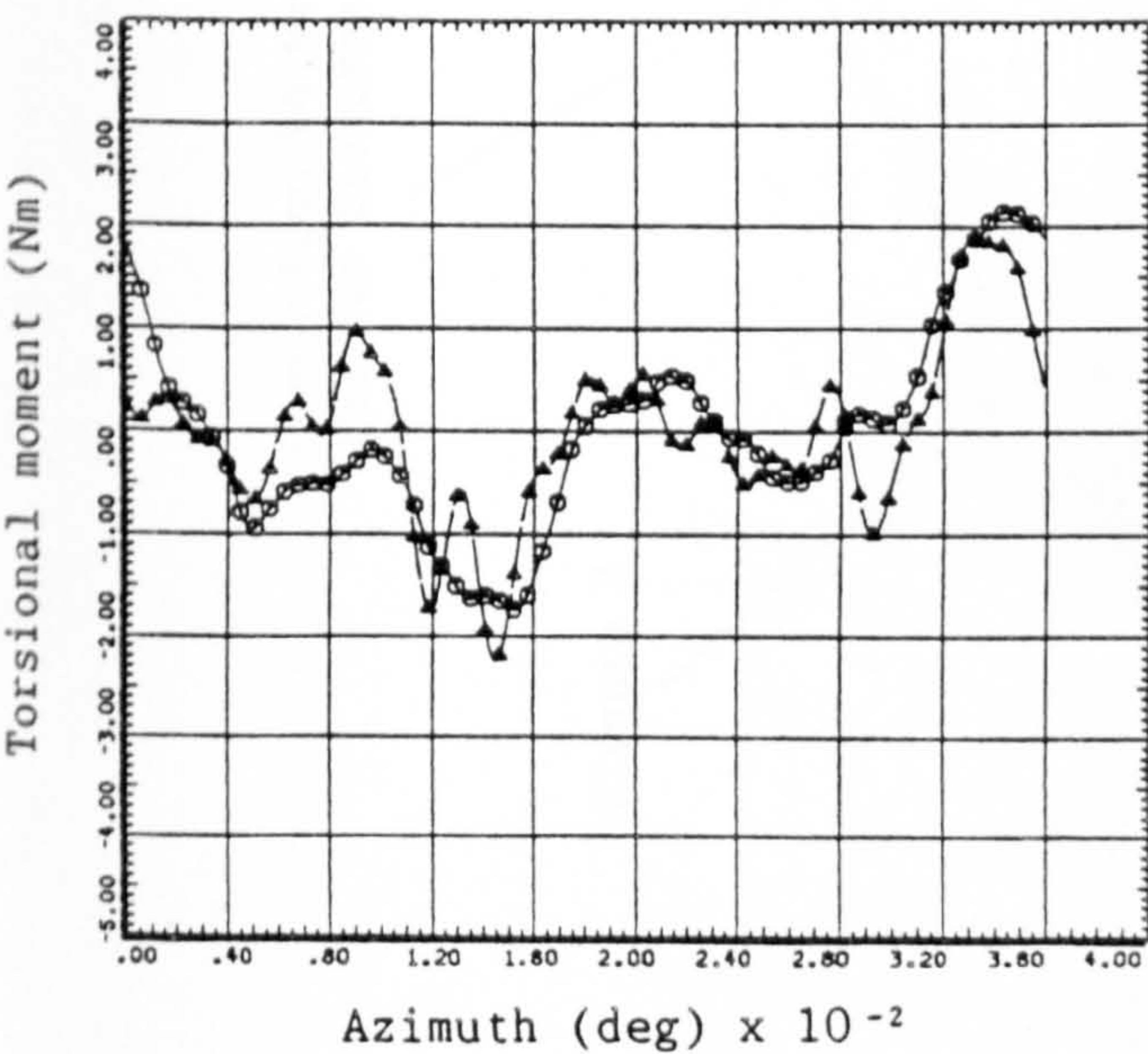
Thrust = 600N



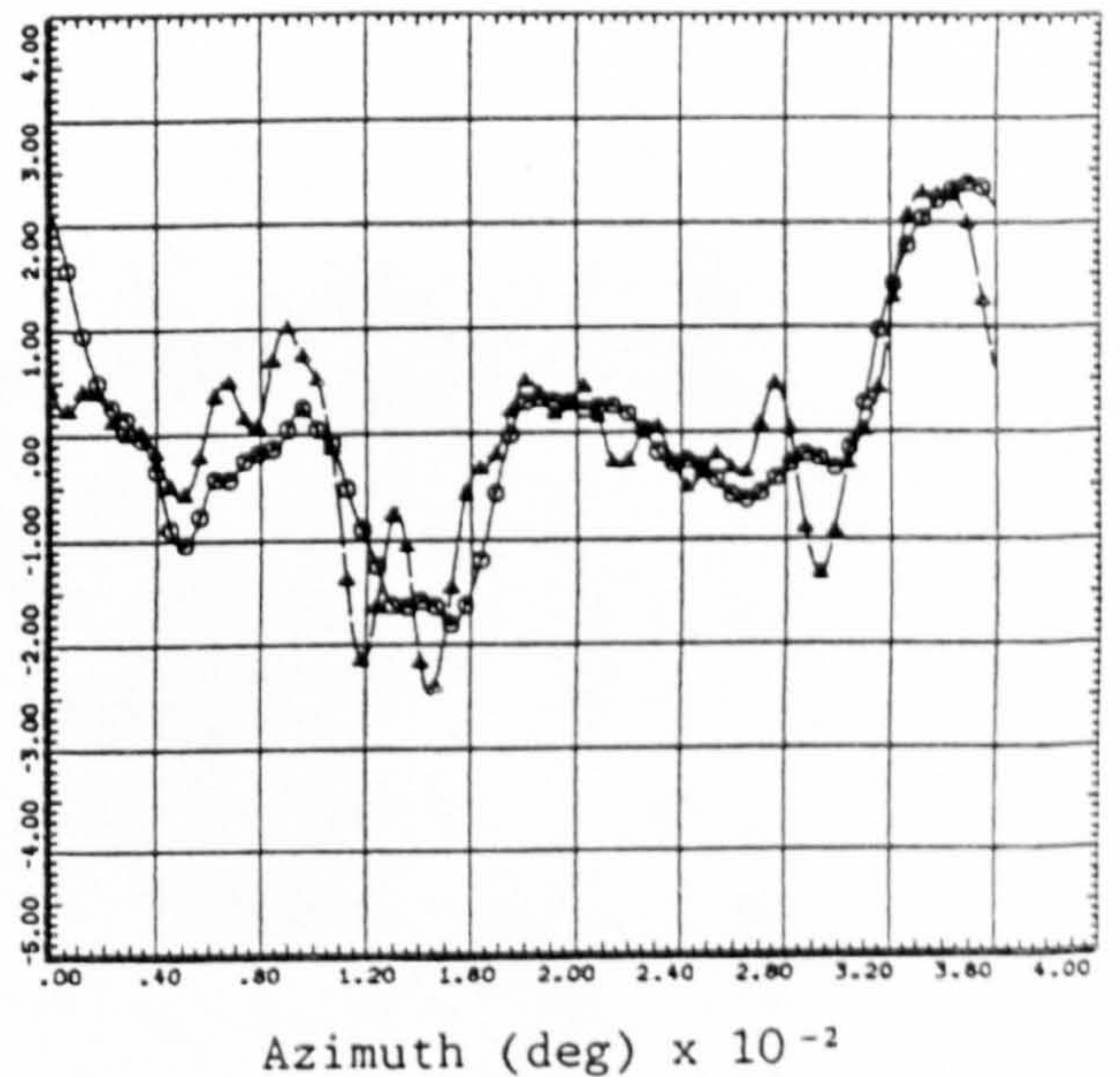
Thrust = 750N



Thrust = 900N



Thrust = 1050N



—○— Experiment

—△— Theory

Fig 61 Measured and predicted (J134) torsional moments for various thrusts, $\mu = 0.2$, 37% R

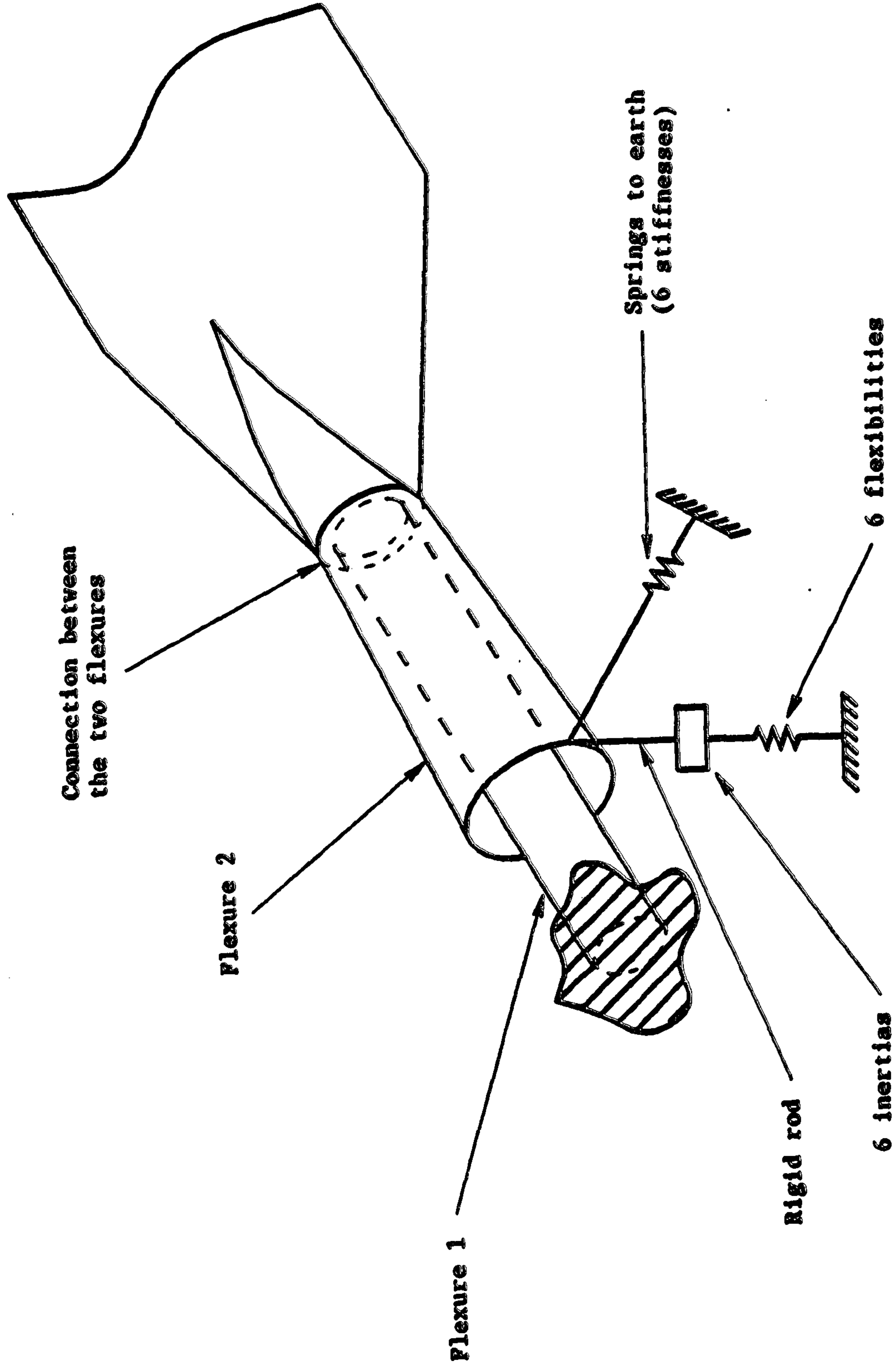


Fig 62 A bearingless main rotor

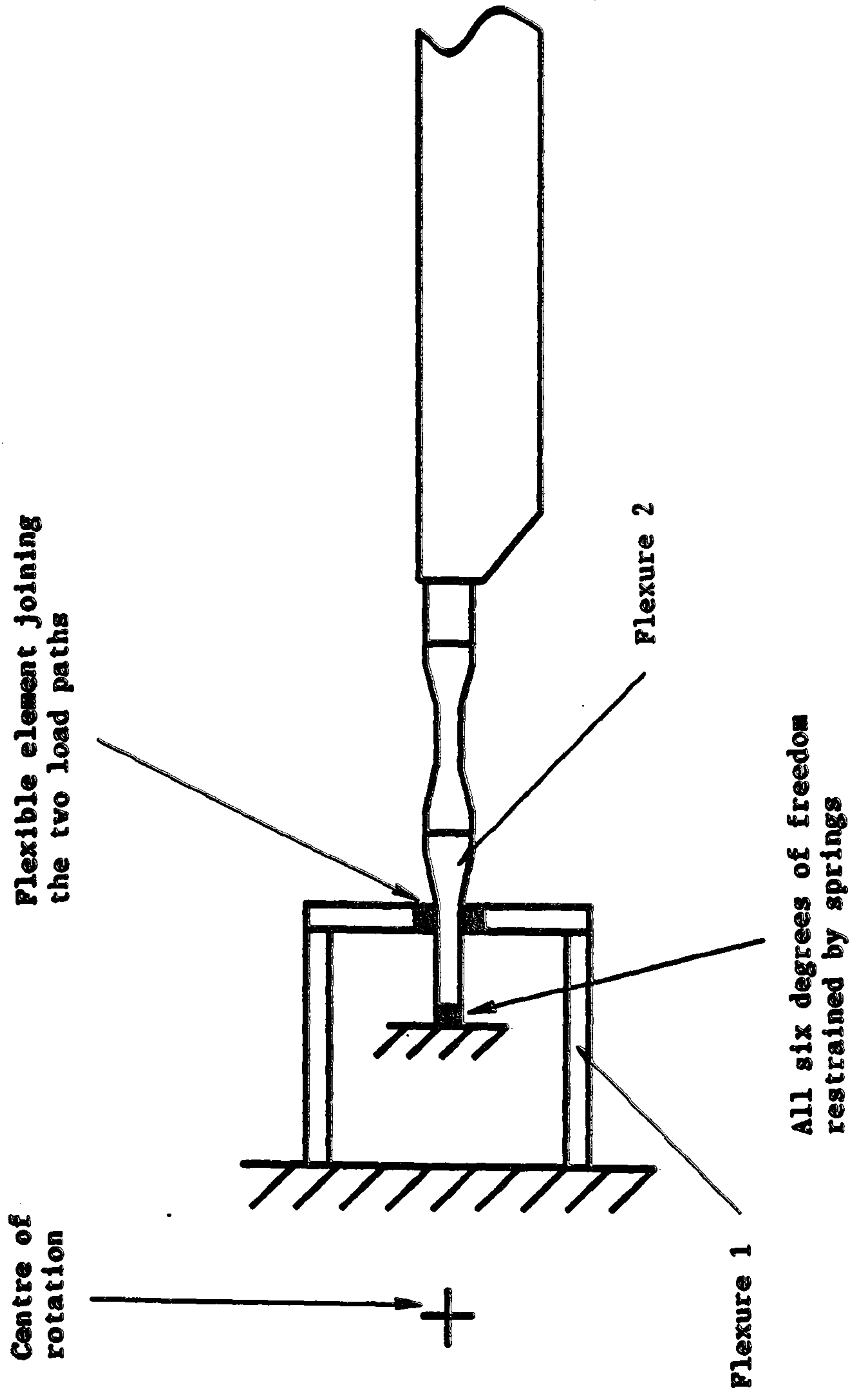


Fig 63 Hub model used in J146 modes program

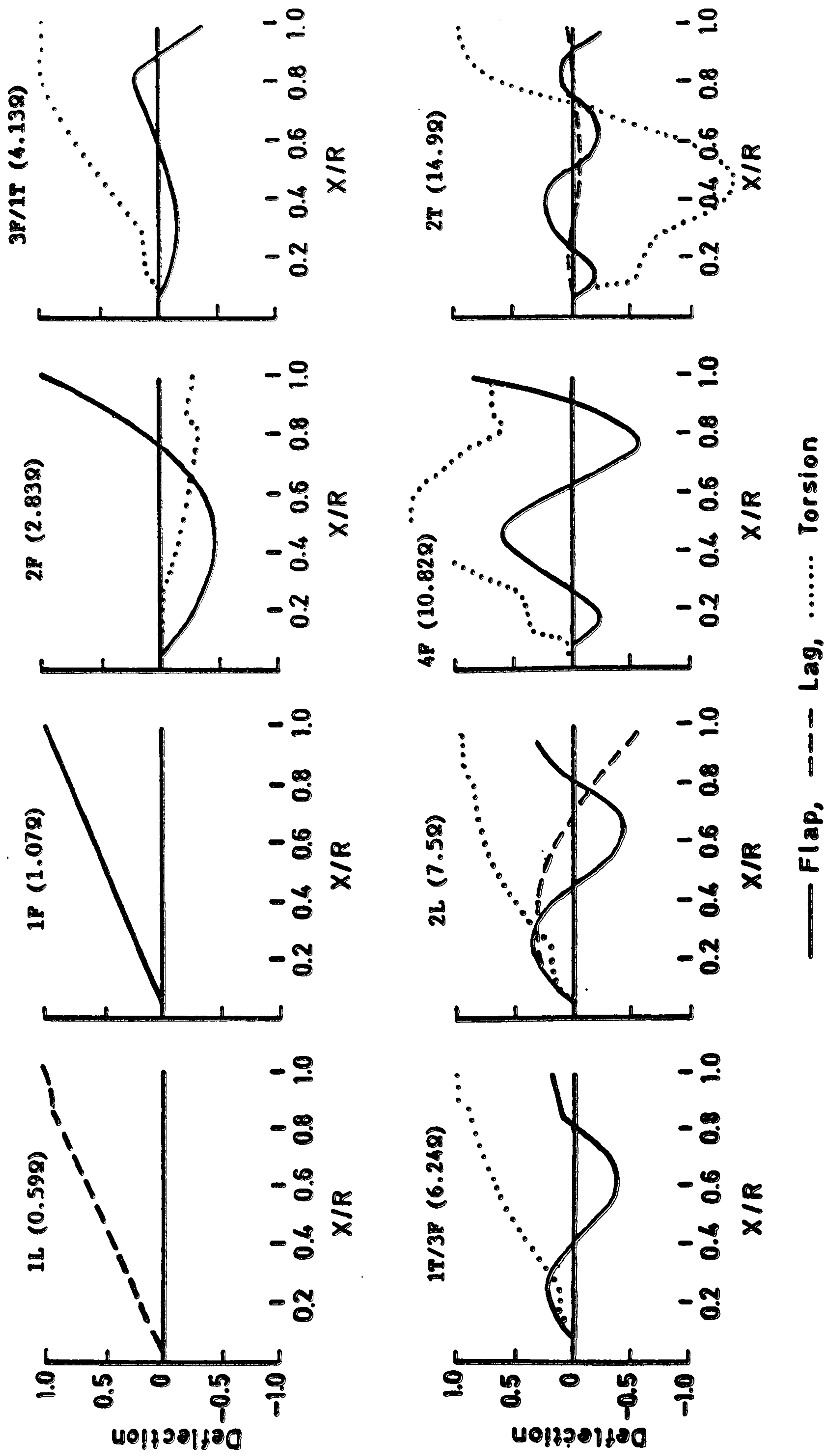


Fig 64 J146 modes shapes for the Mk2 swept tip blade (600 rev/min)

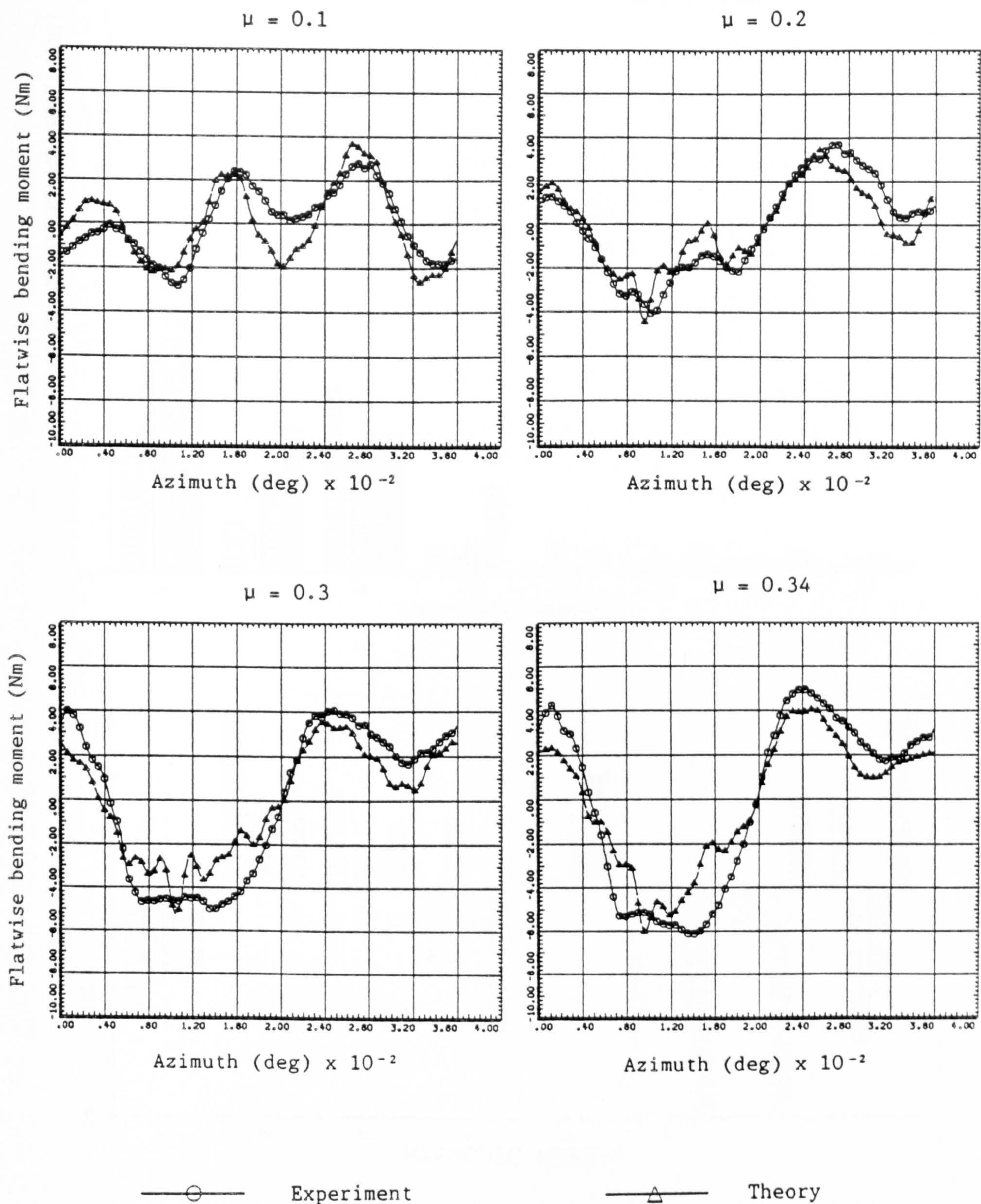


Fig 65 Measured and predicted (J146) flatwise bending moments for various advance ratios, thrust = 900N, 37% R

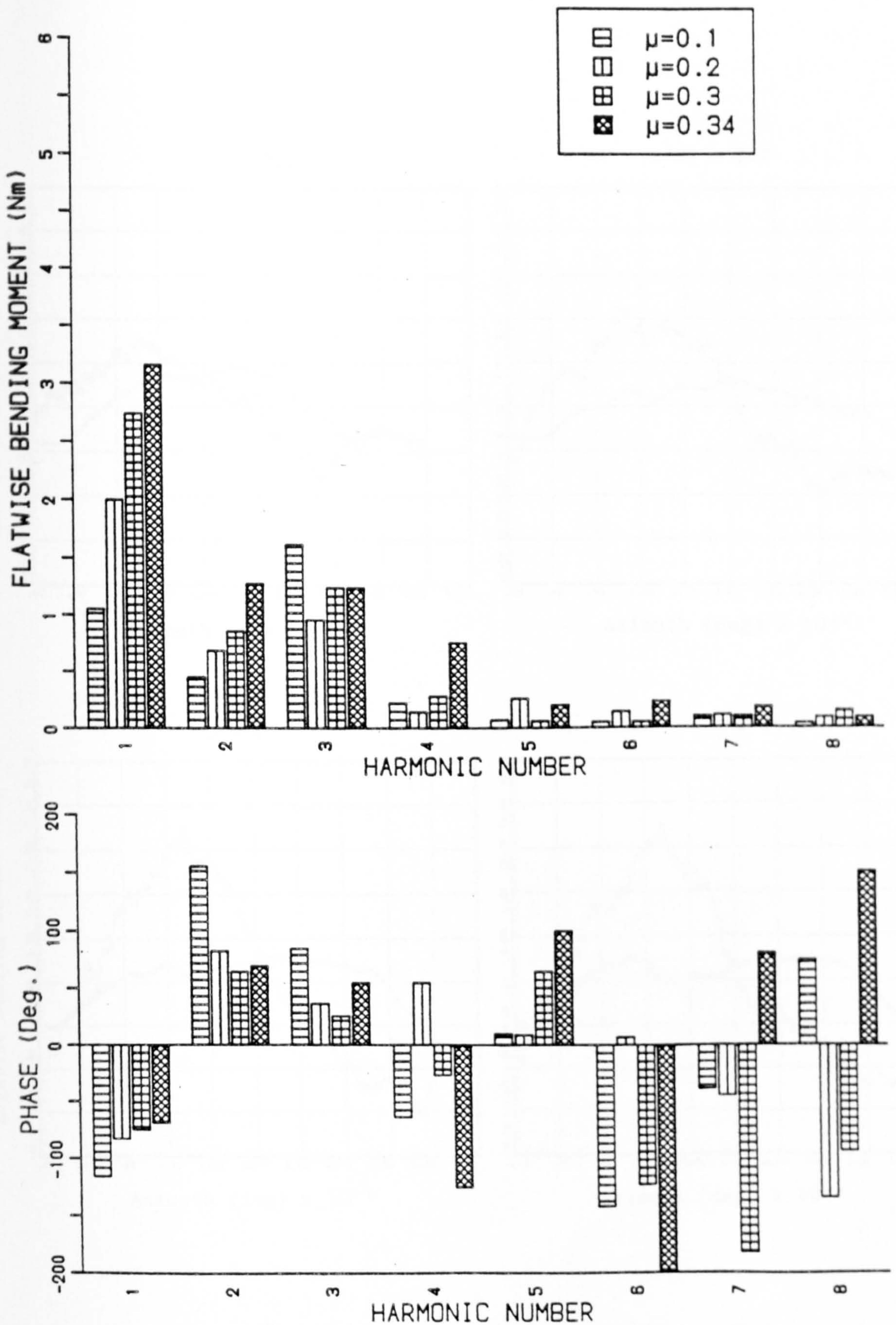
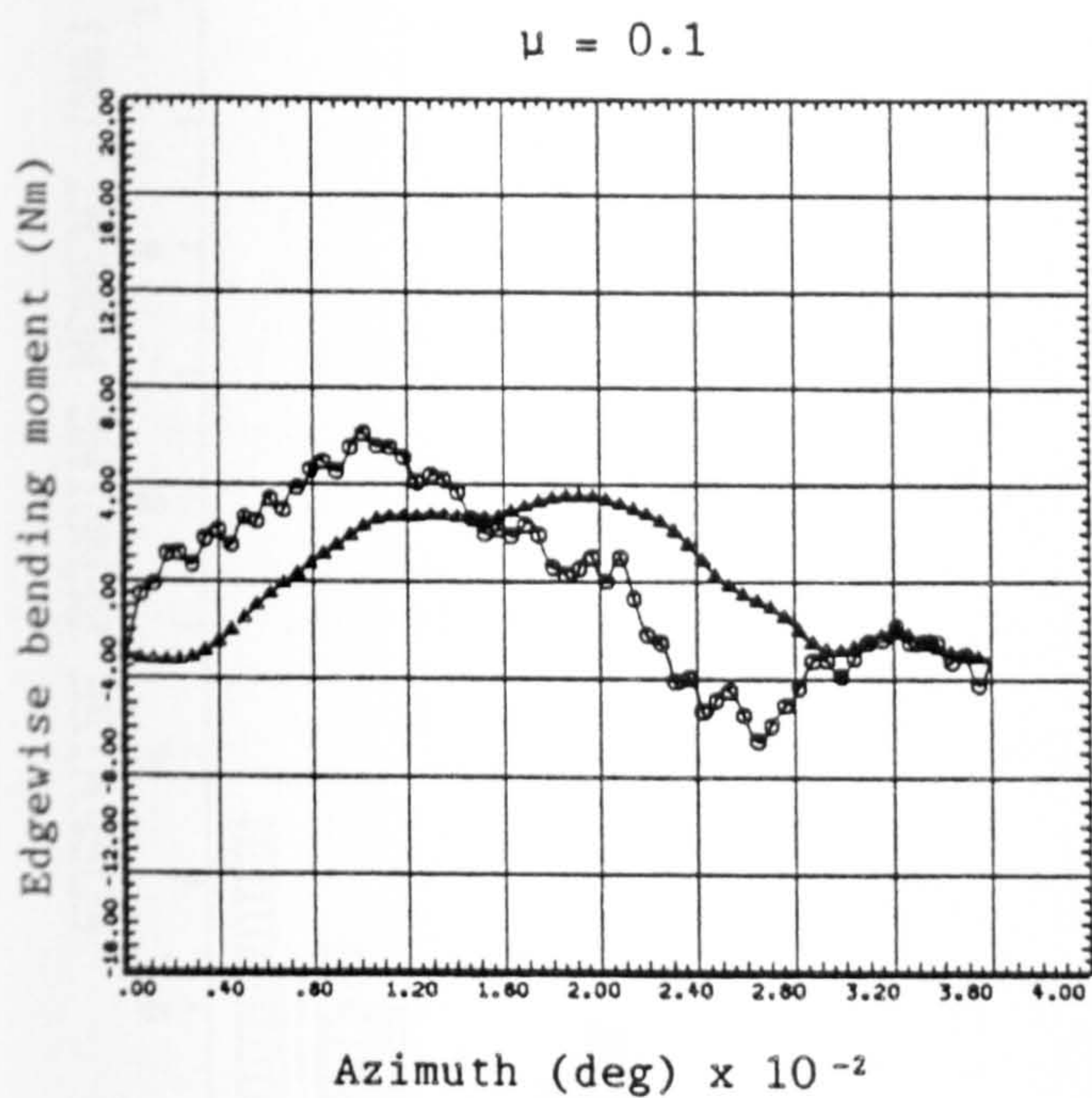
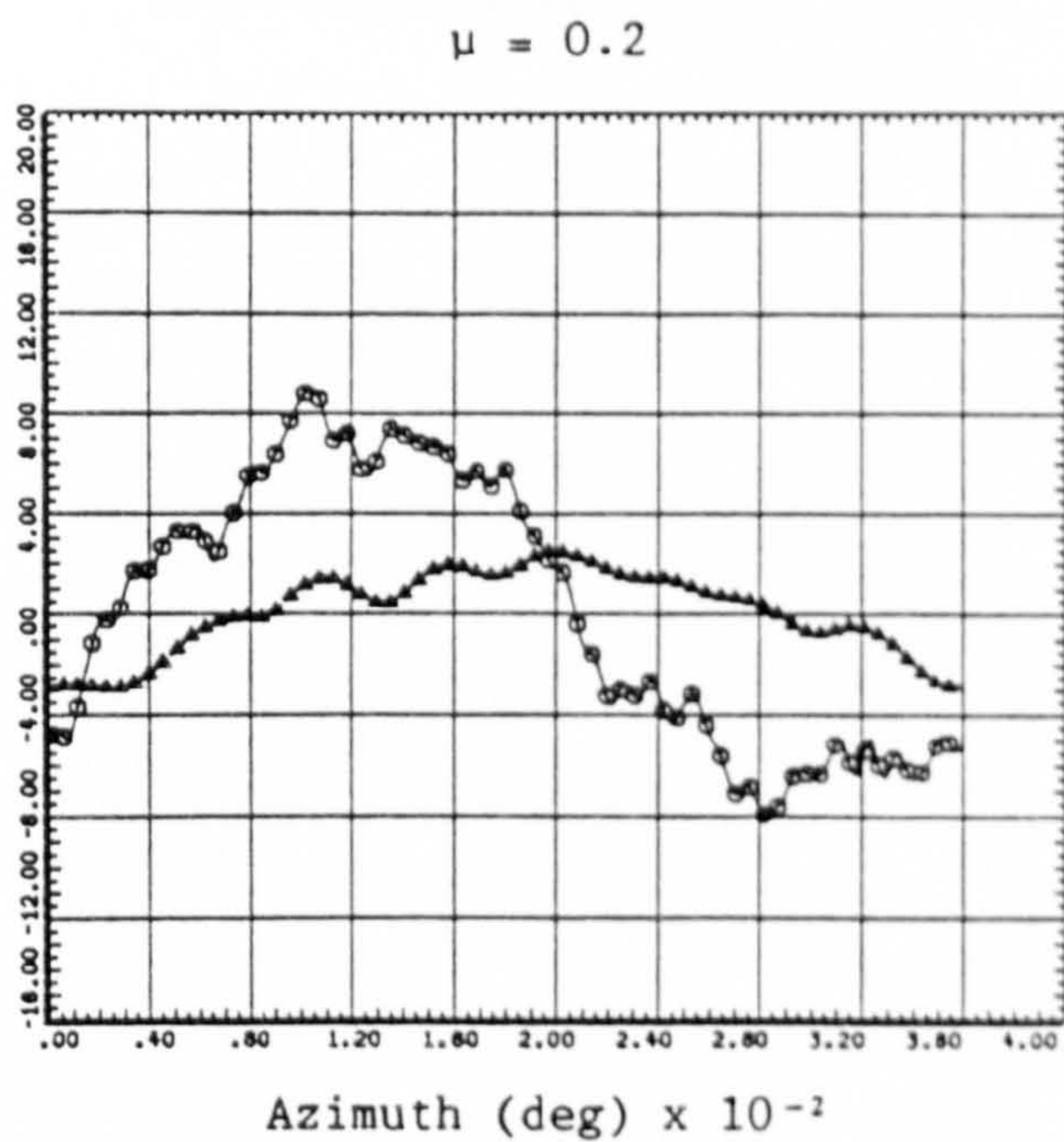
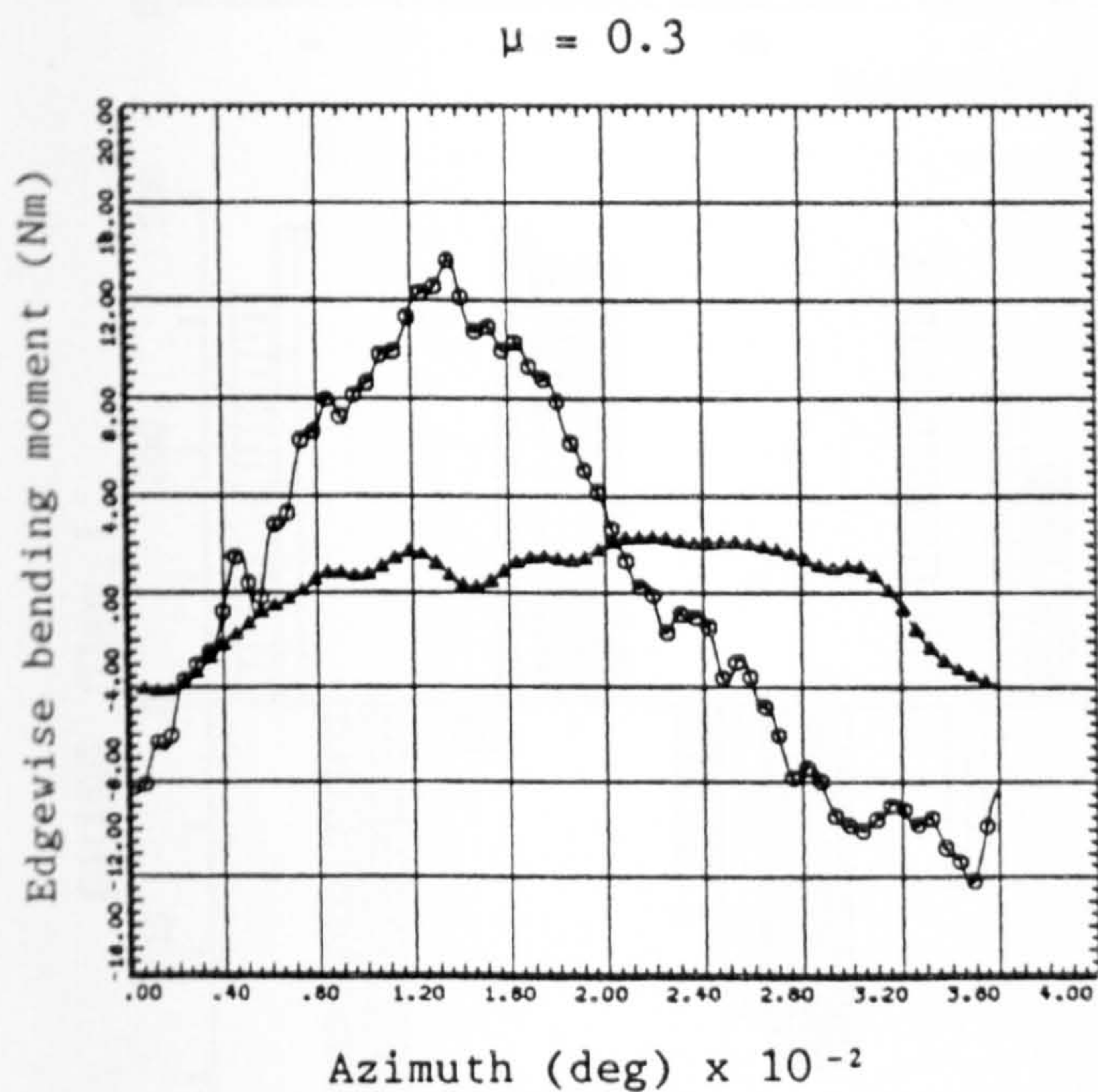
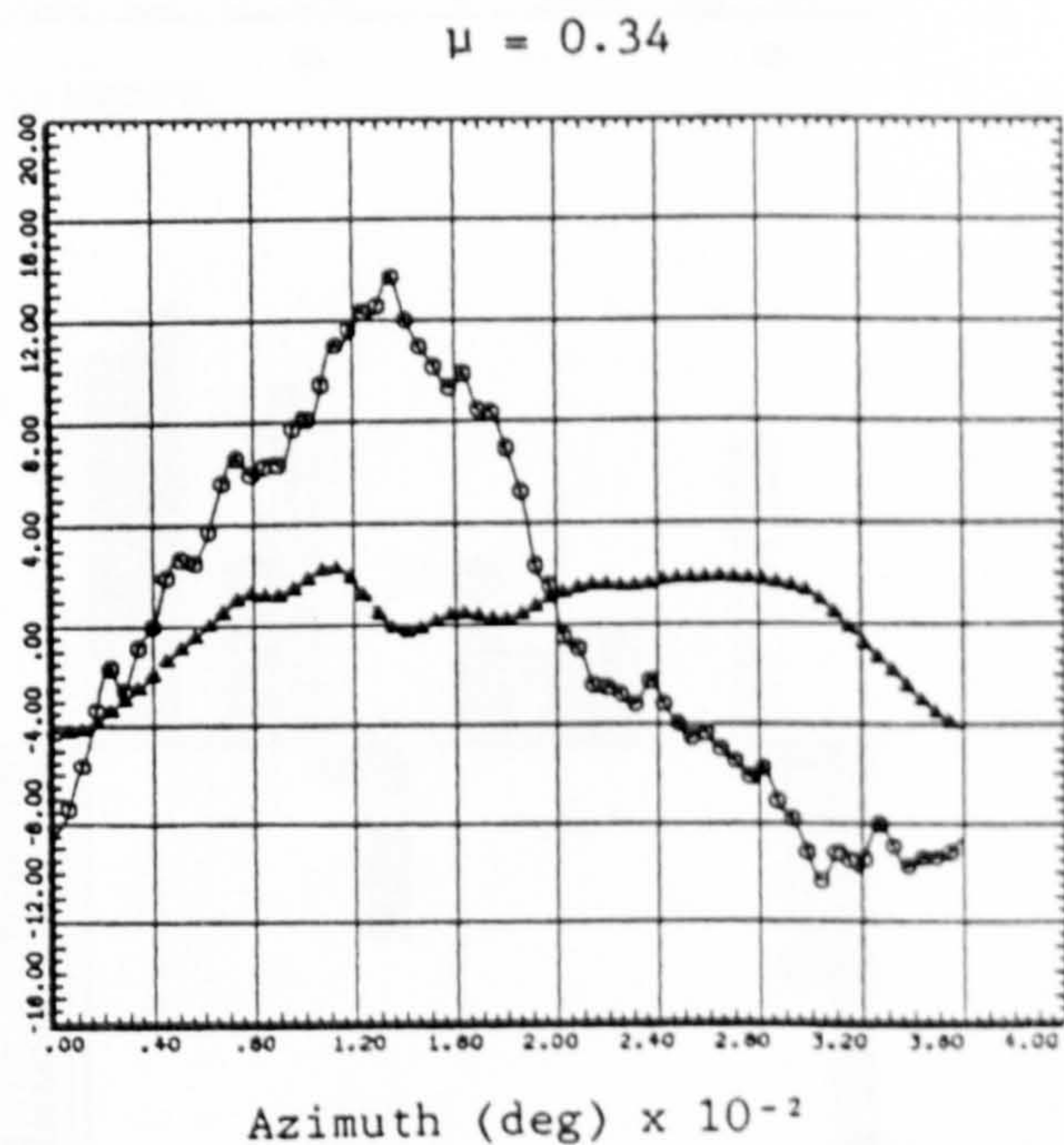


Fig 66 Harmonic analysis of the predicted (J146) flatwise bending moments on the Mk2 swept tip blade, thrust = 900N, 37% R



—○— Experiment

—△— Theory

Fig 67 Measured and predicted (J146) edgewise bending moments for various advance ratios, thrust = 900N, 37% R

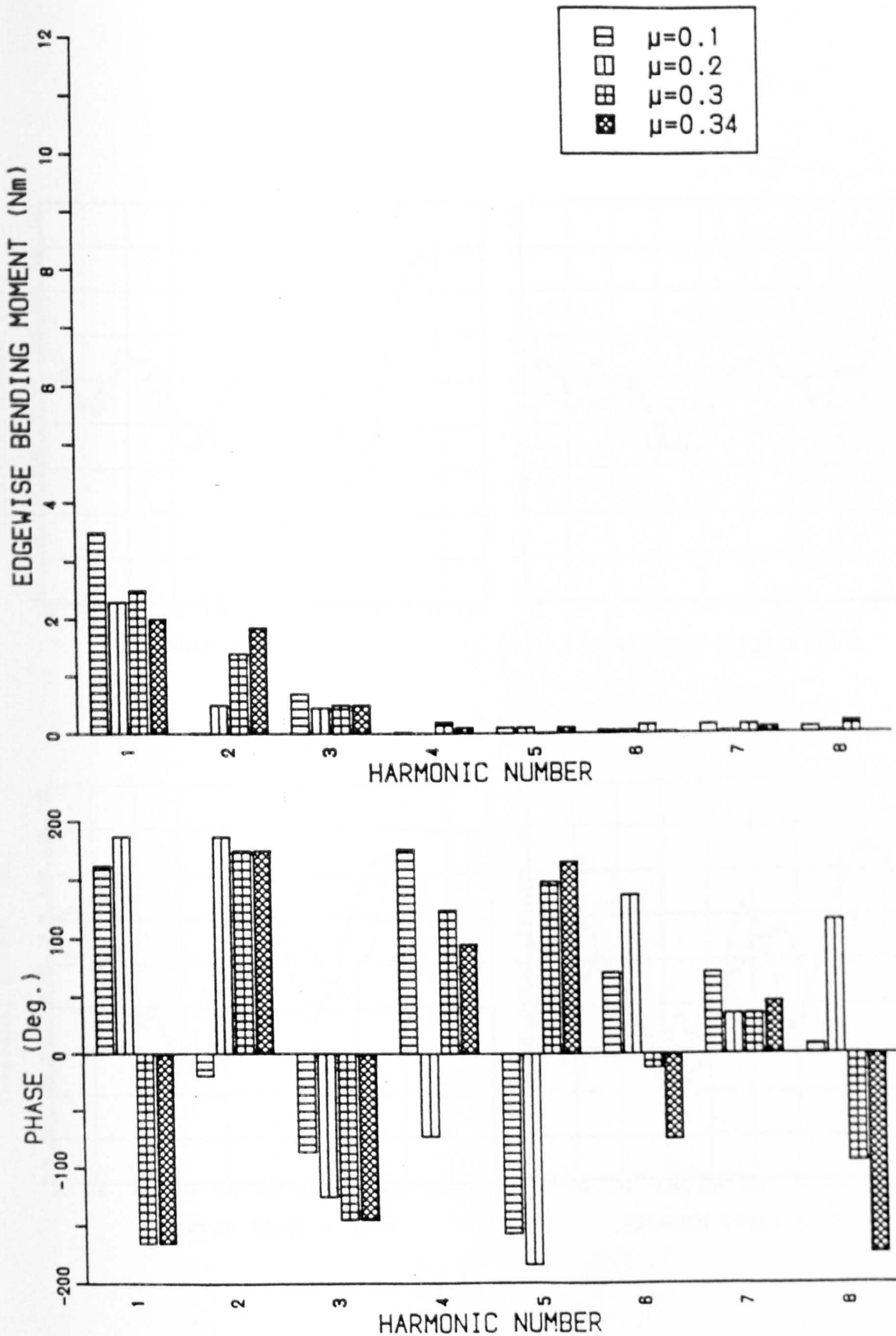
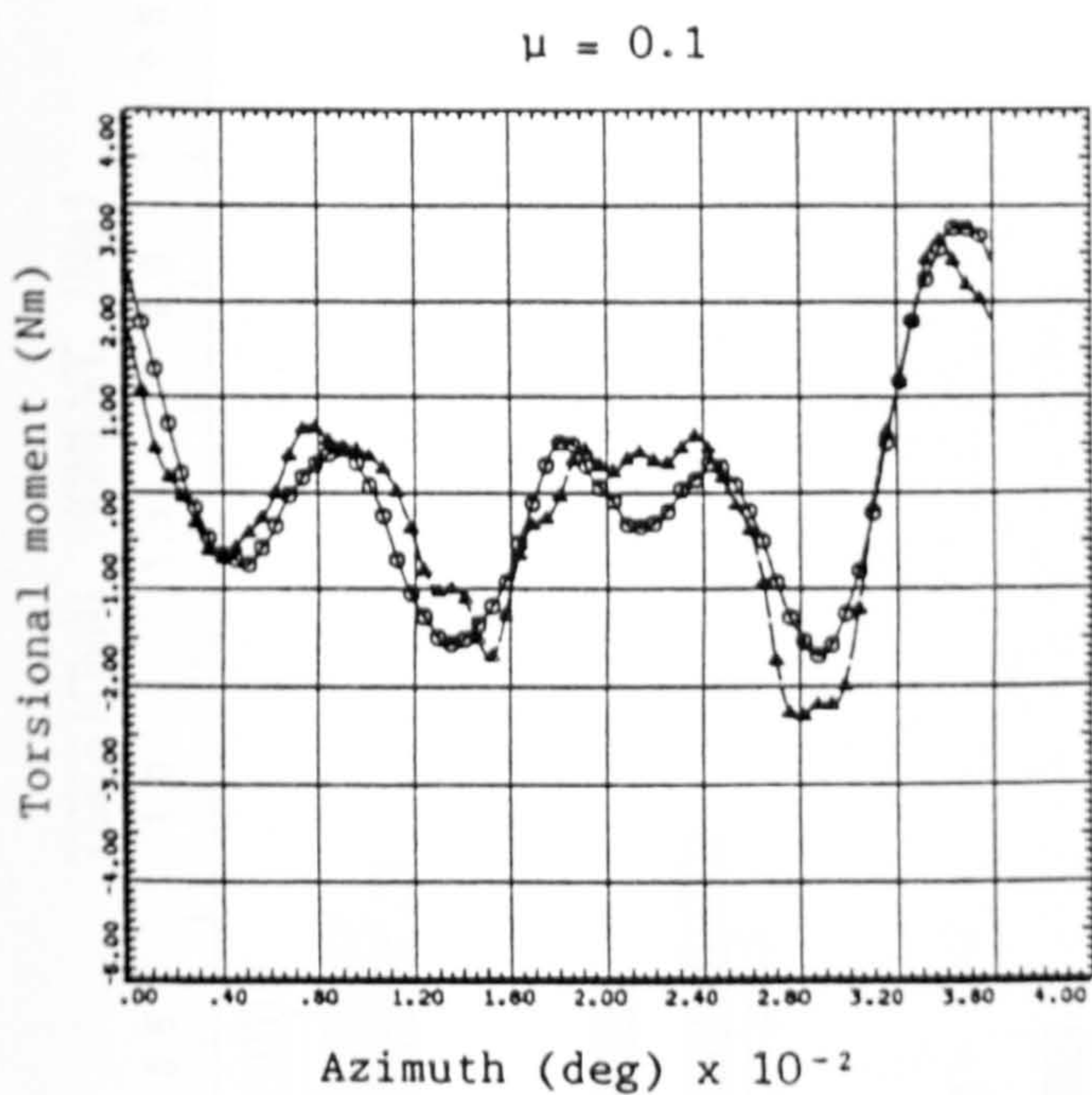
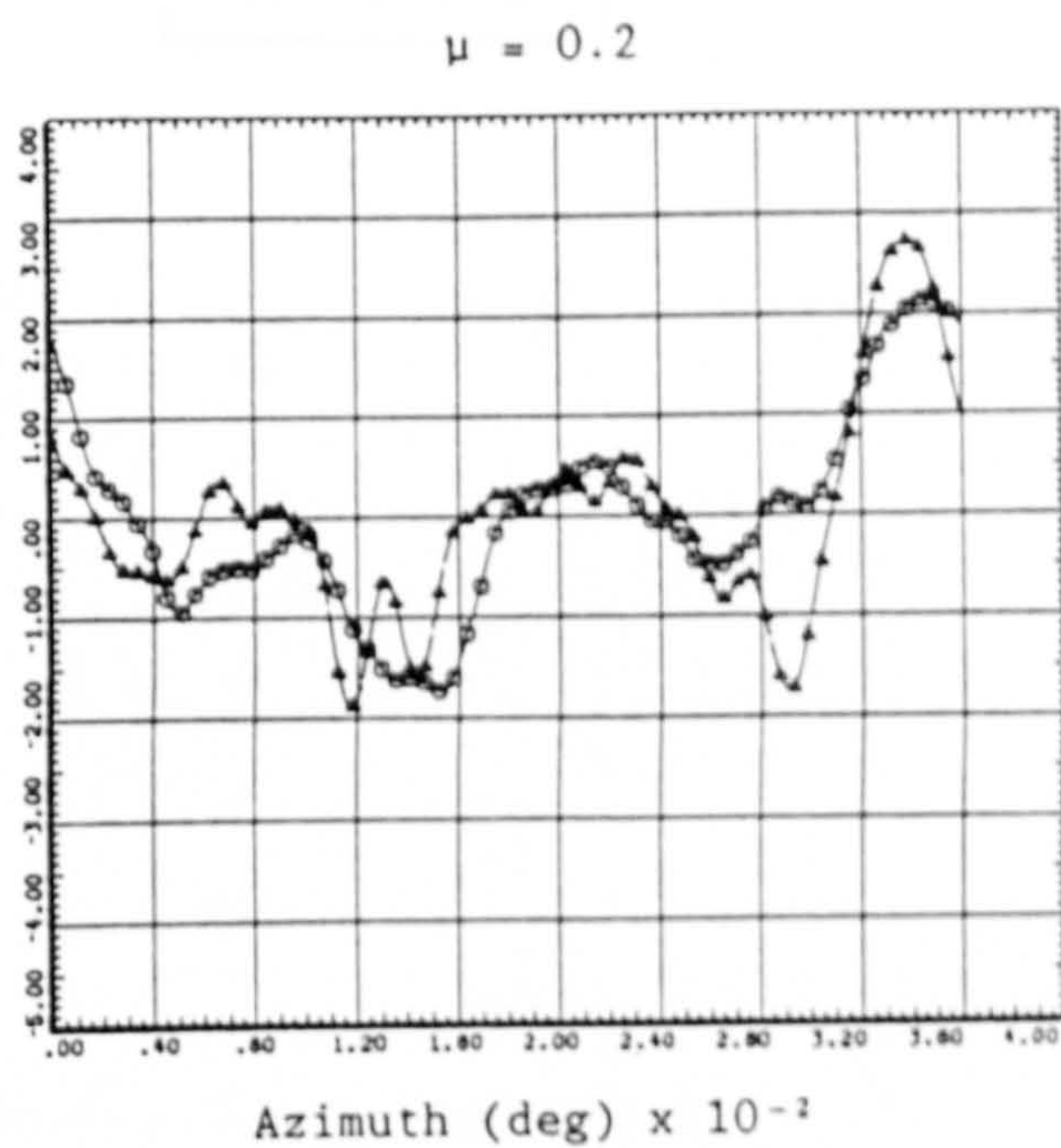
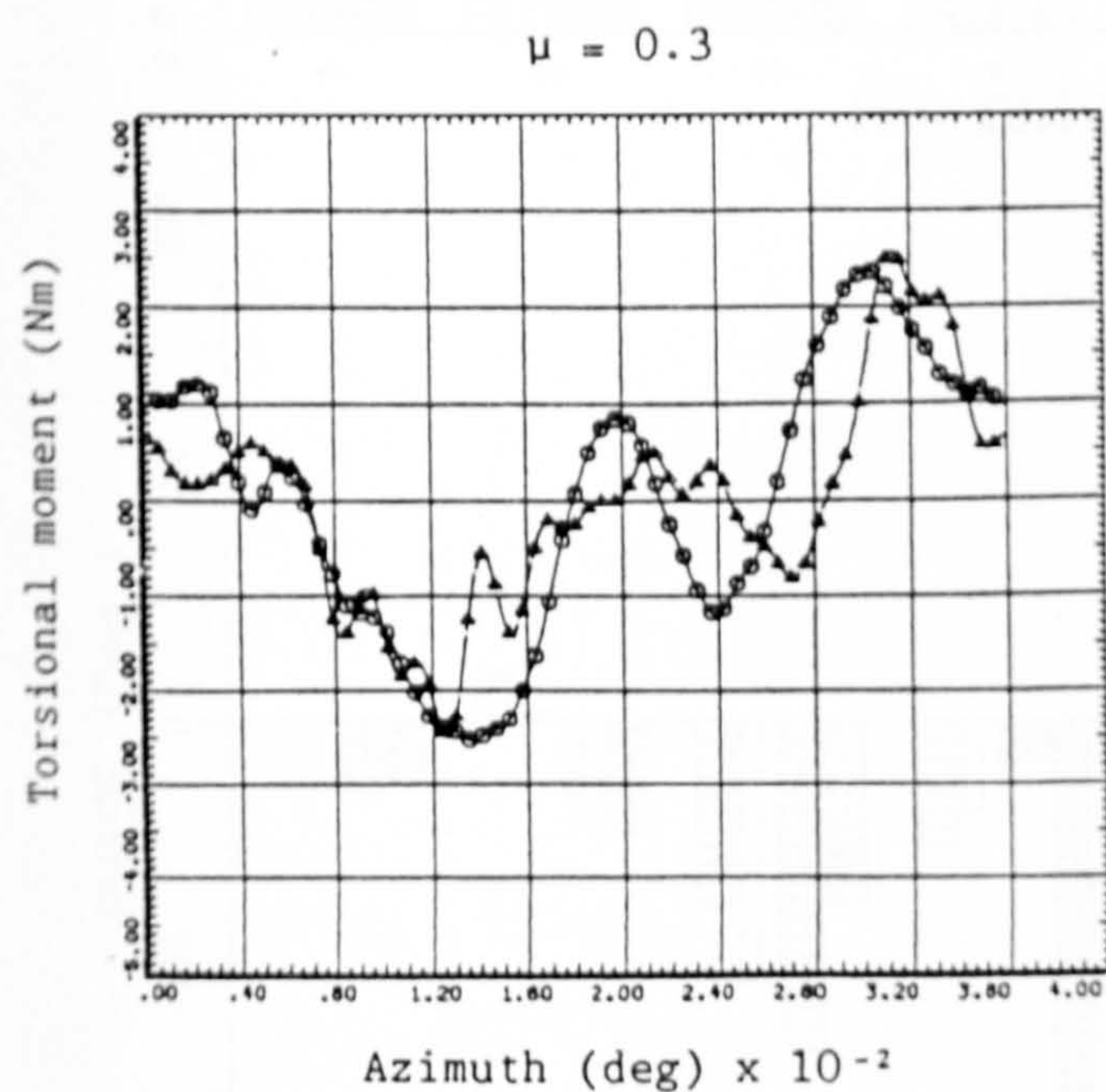
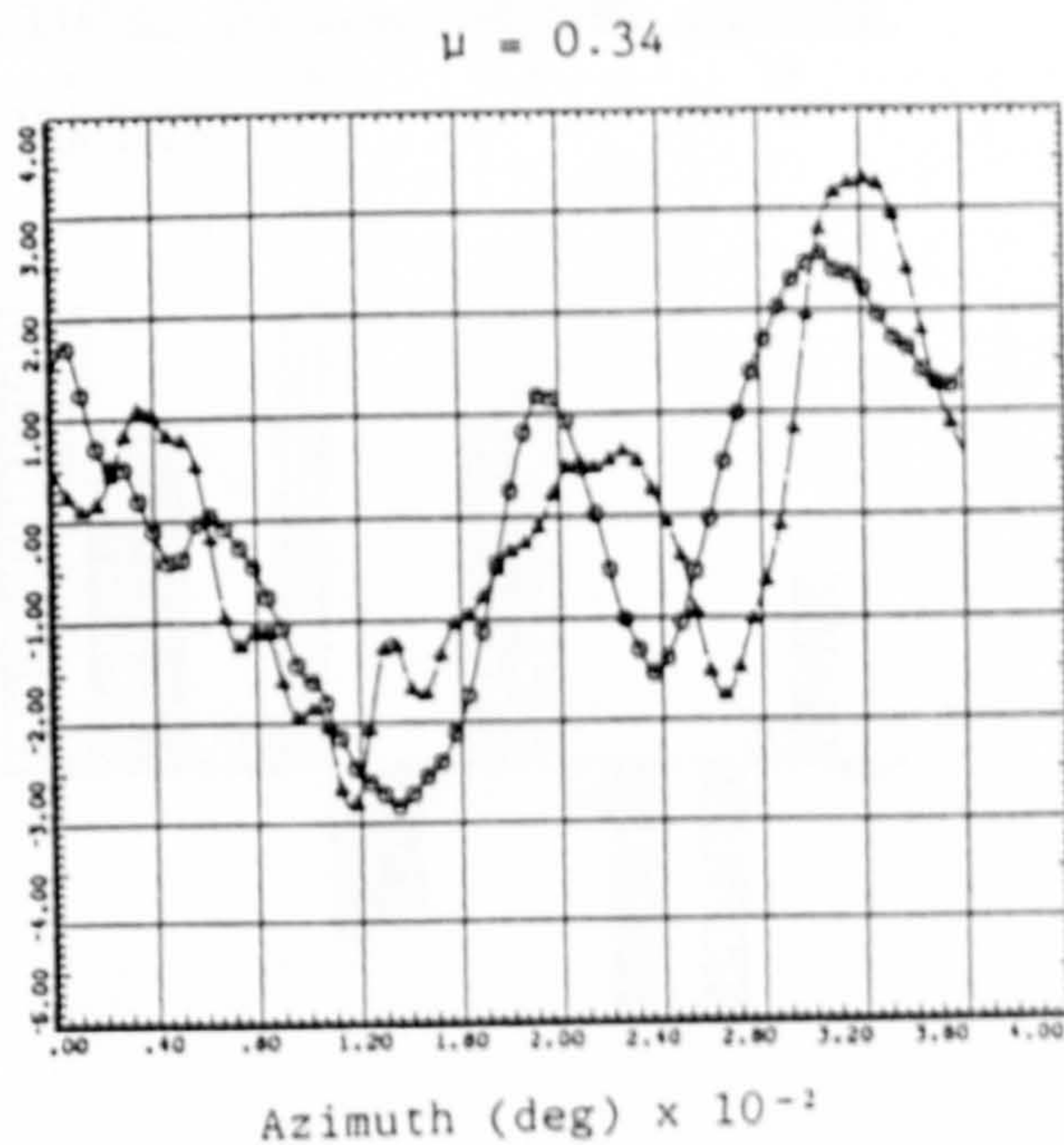


Fig 68 Harmonic analysis of the predicted (J146) edgewise bending moments on the Mk2 swept tip blade, thrust = 900N, 37% R



—○— Experiment

—△— Theory

Fig 69 Measured and predicted (J146) torsional moments for various advance ratios, thrust = 900N, 37% R

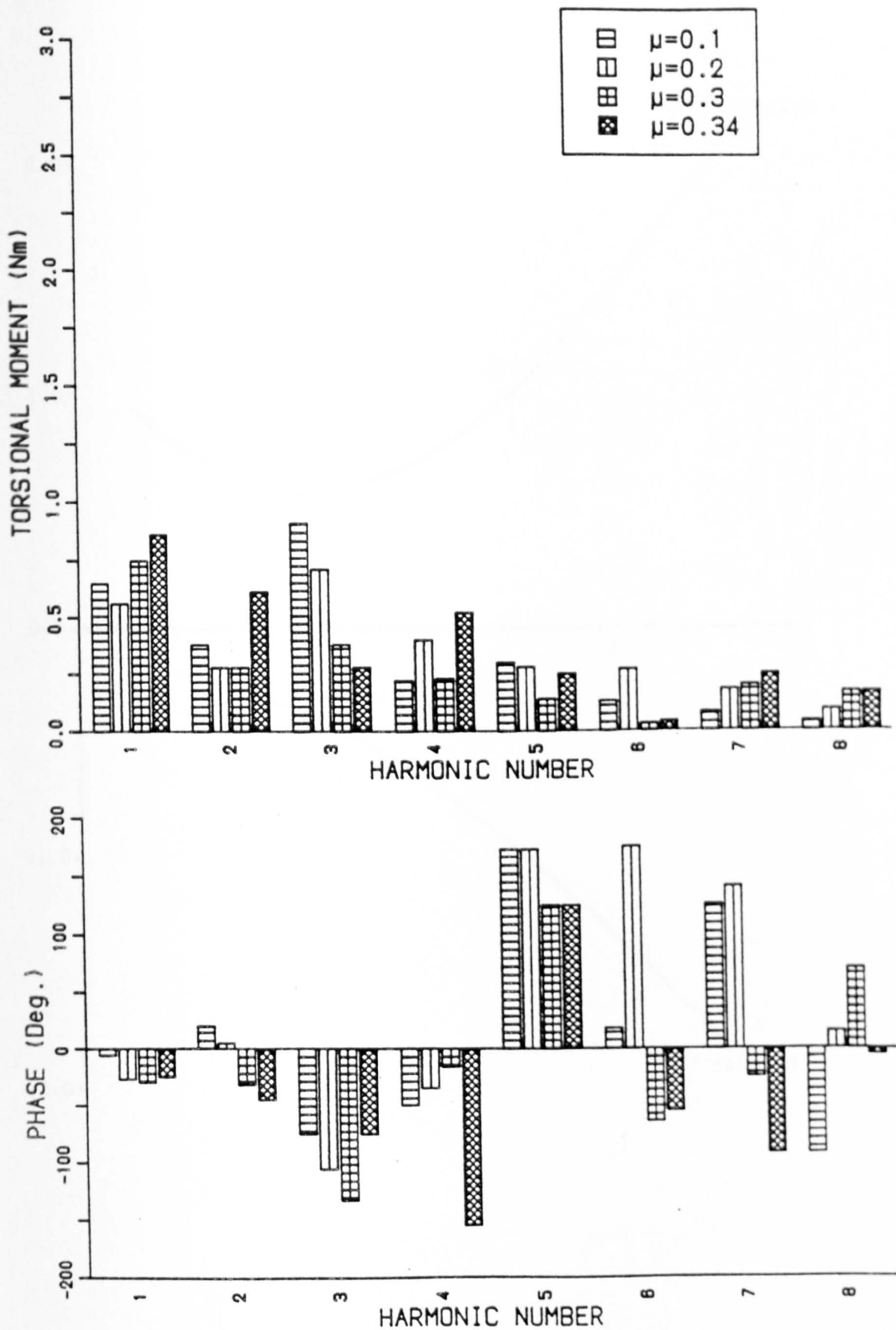


Fig 70 Harmonic analysis of the predicted (J146) torsional moments on the Mk2 swept tip blade, thrust = 900N, 37% R

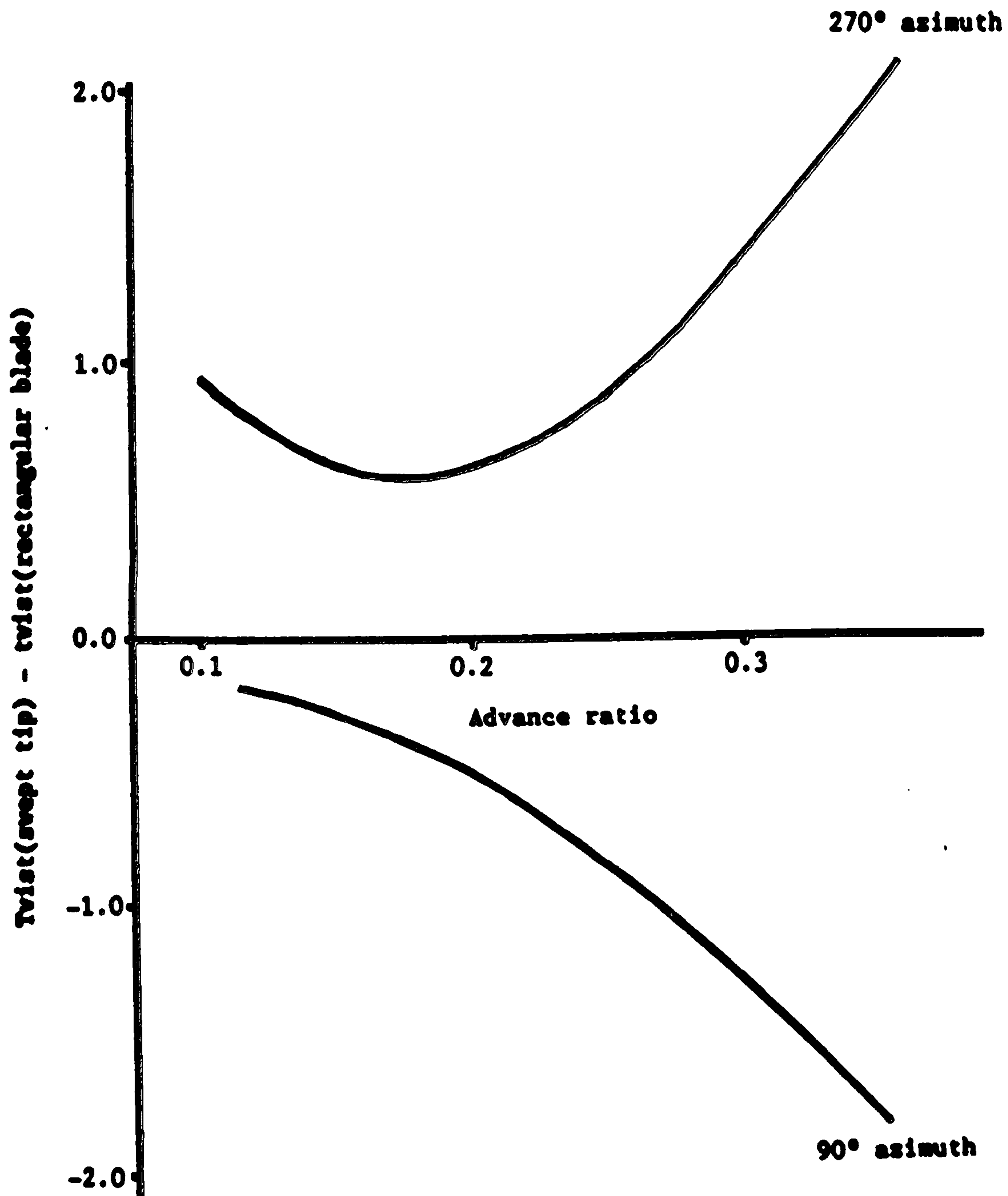


Fig 71 Predicted twist of the Mk2 swept tip blade relative to the twist of the baseline blade, thrust = 900N

Leading edge extension

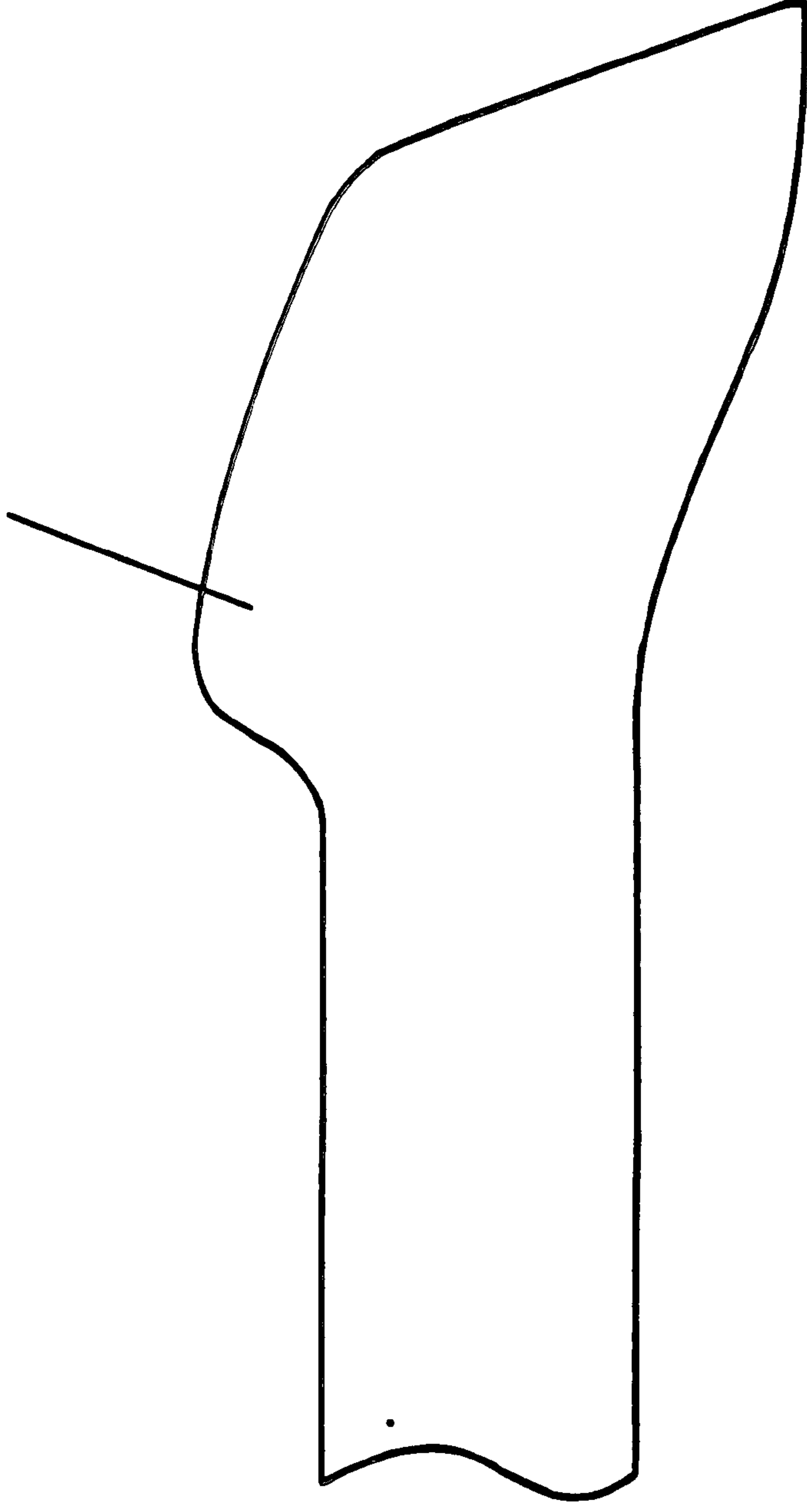


Fig 72 BERP3 blade tip

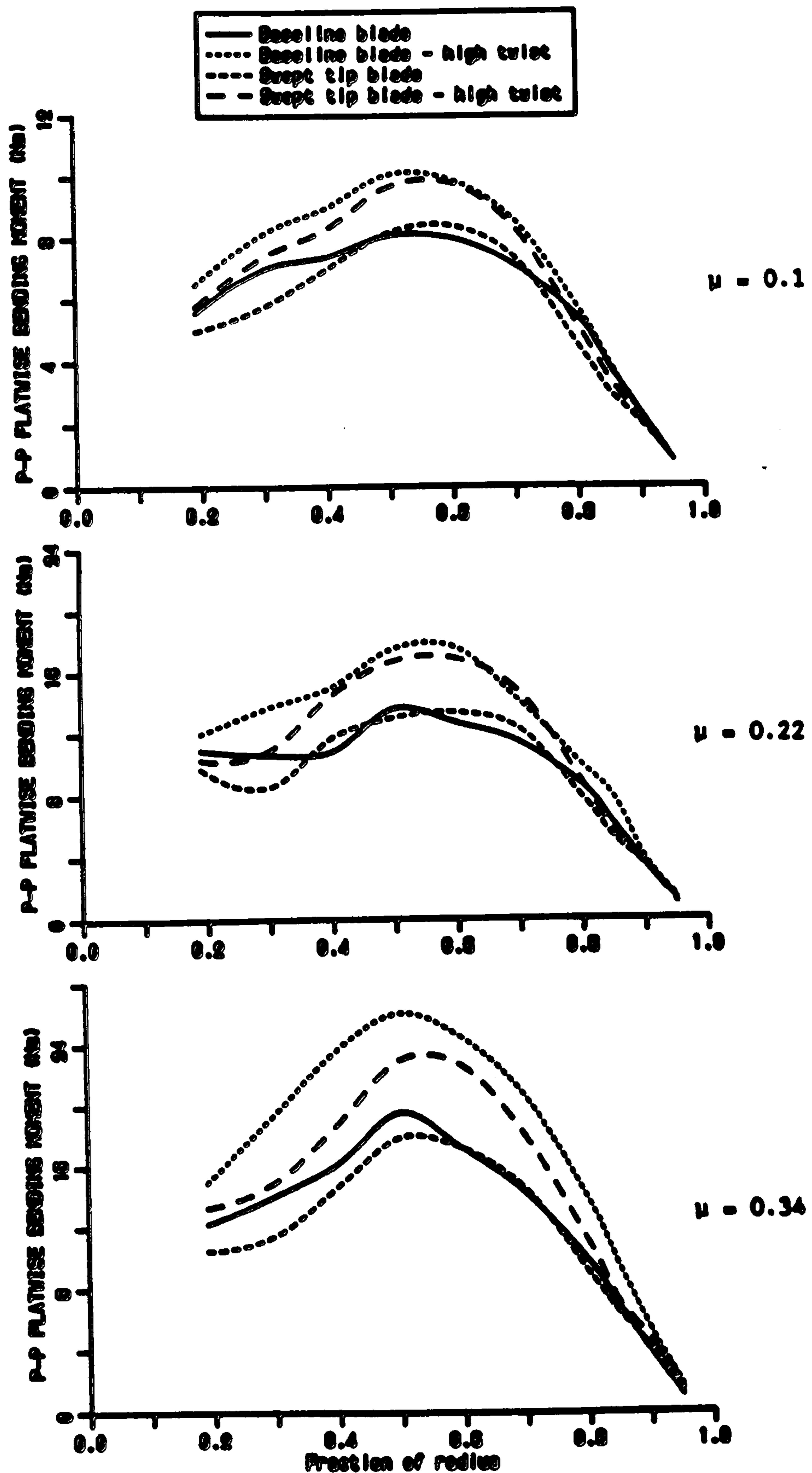


Fig 73 Predicted (J146) peak-to-peak flatwise bending moments for various blades, thrust = 900N.

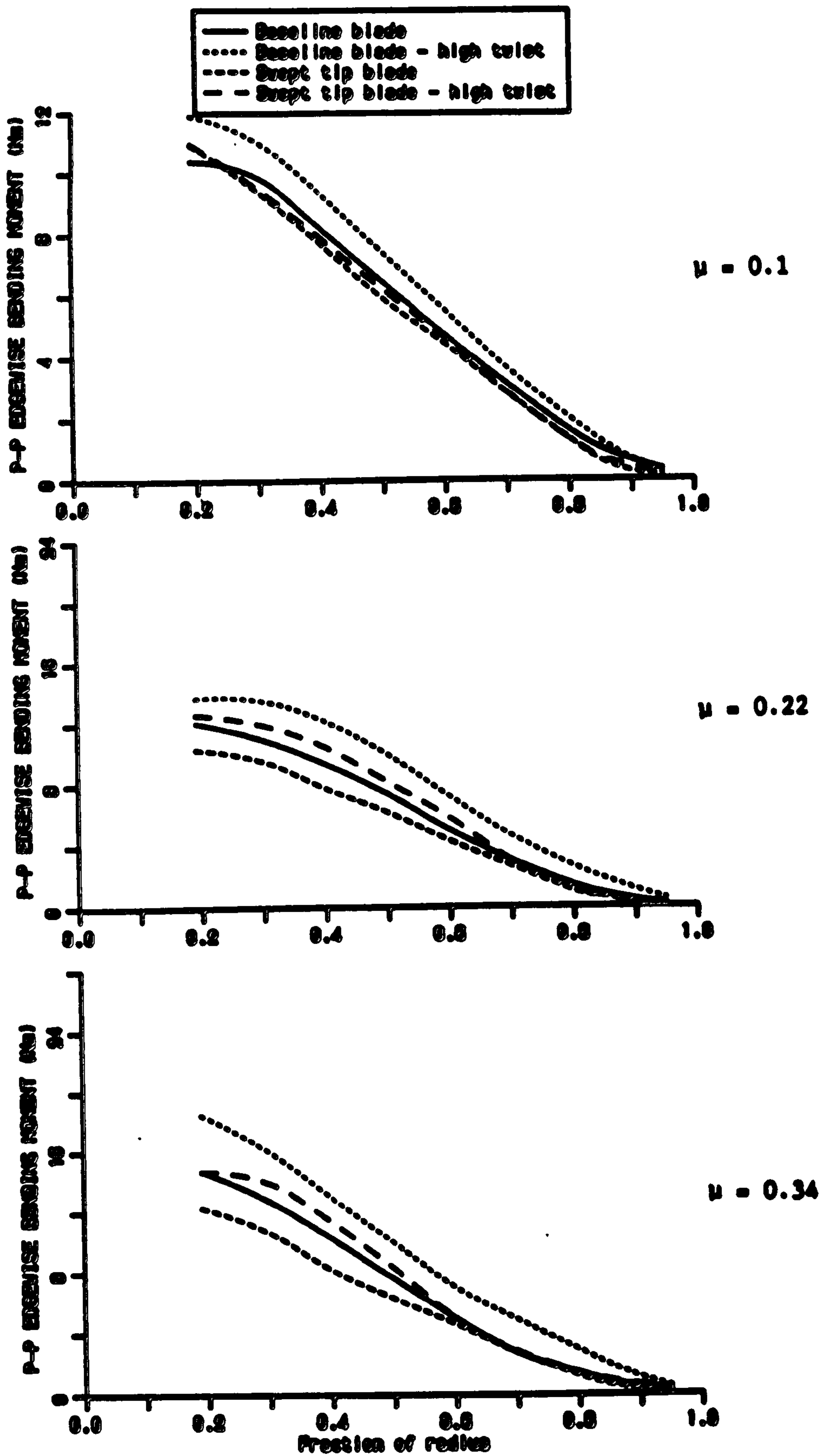


Fig 74 Predicted (J146) peak-to-peak edgewise bending moments for various blades, thrust = 900N.

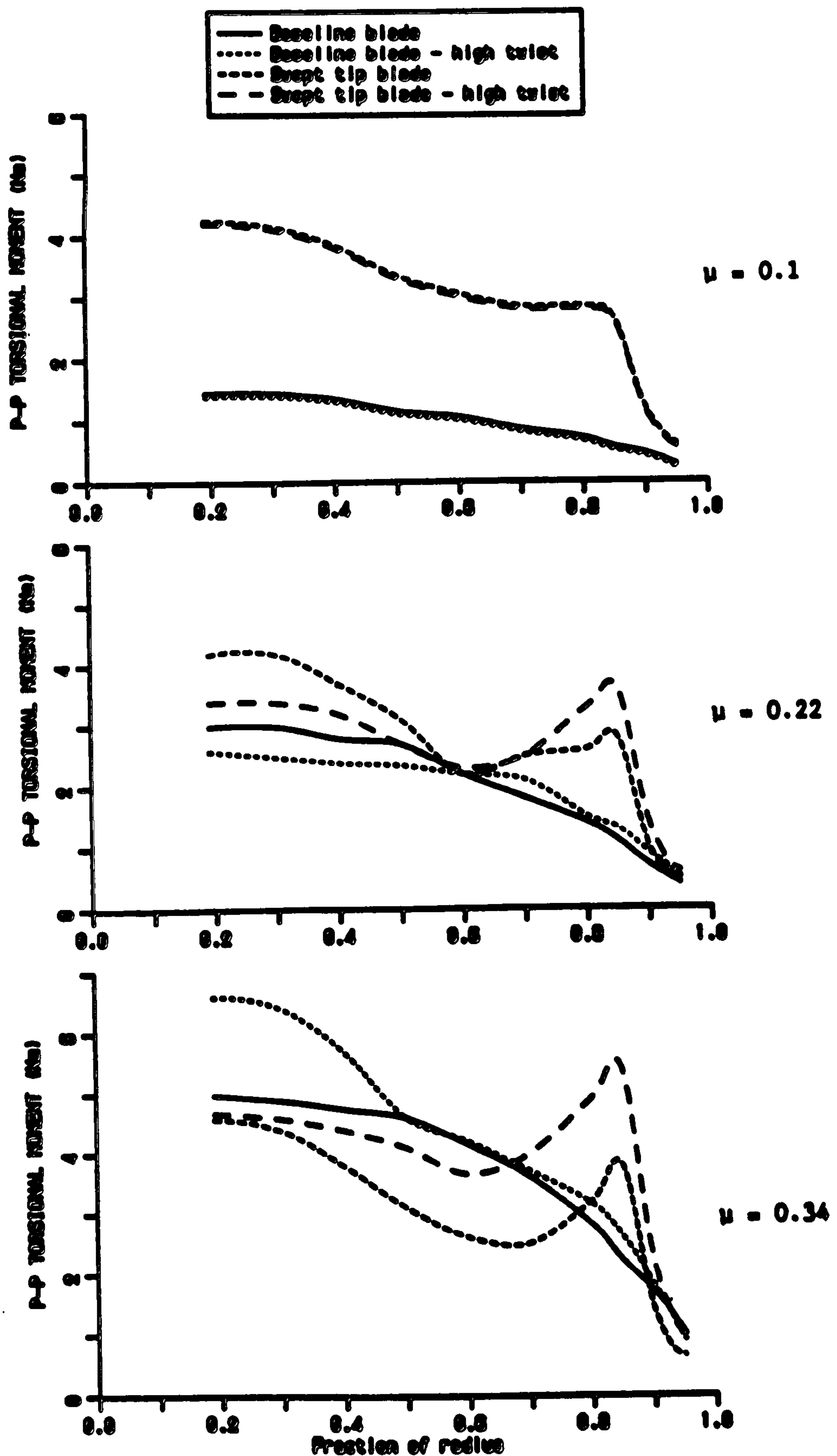


Fig 75 Predicted (J146) peak-to-peak torsional moments for various blades, thrust = 900N.

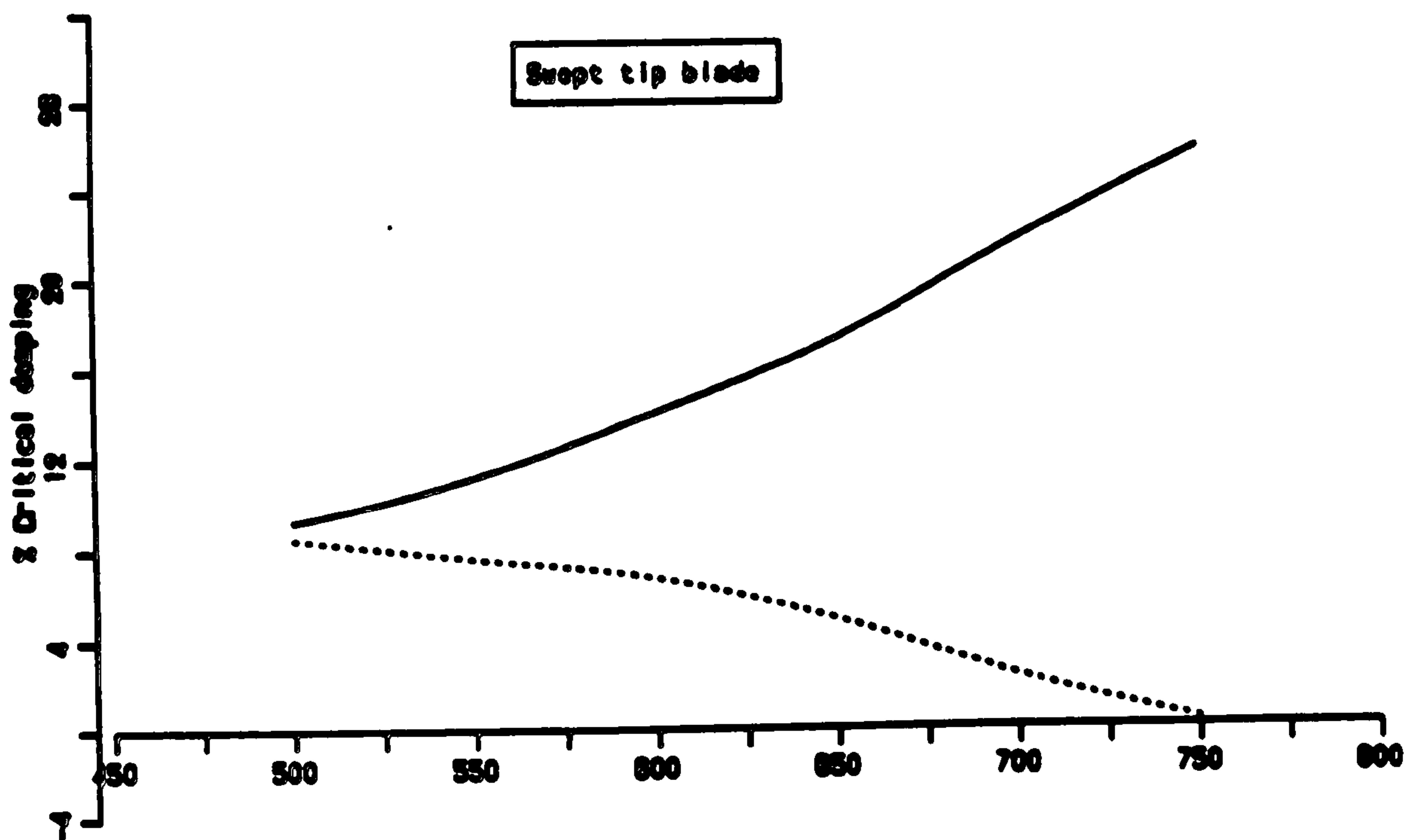
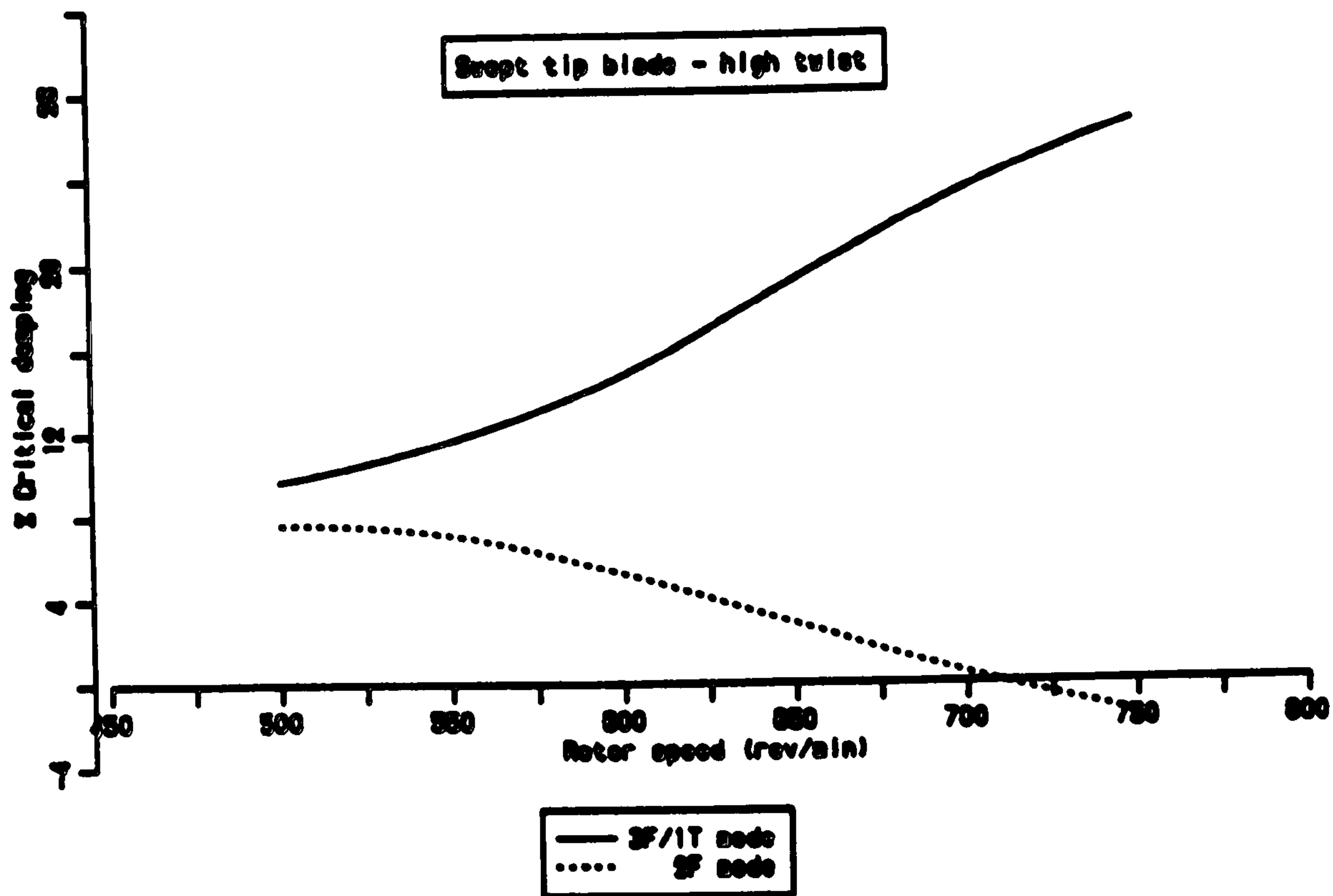


Fig 76 The effect of increased twist on modal damping

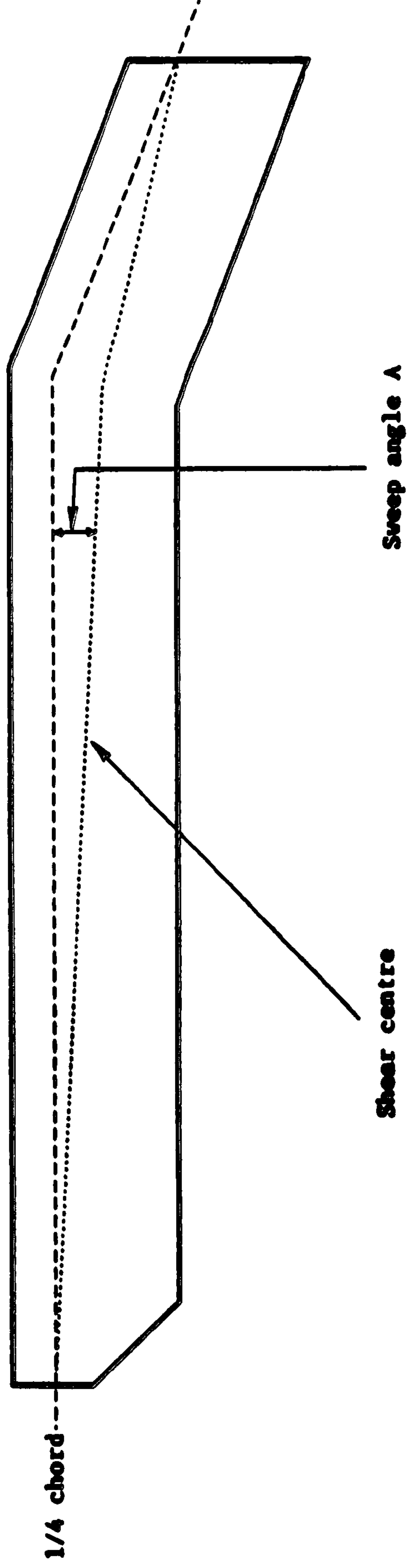
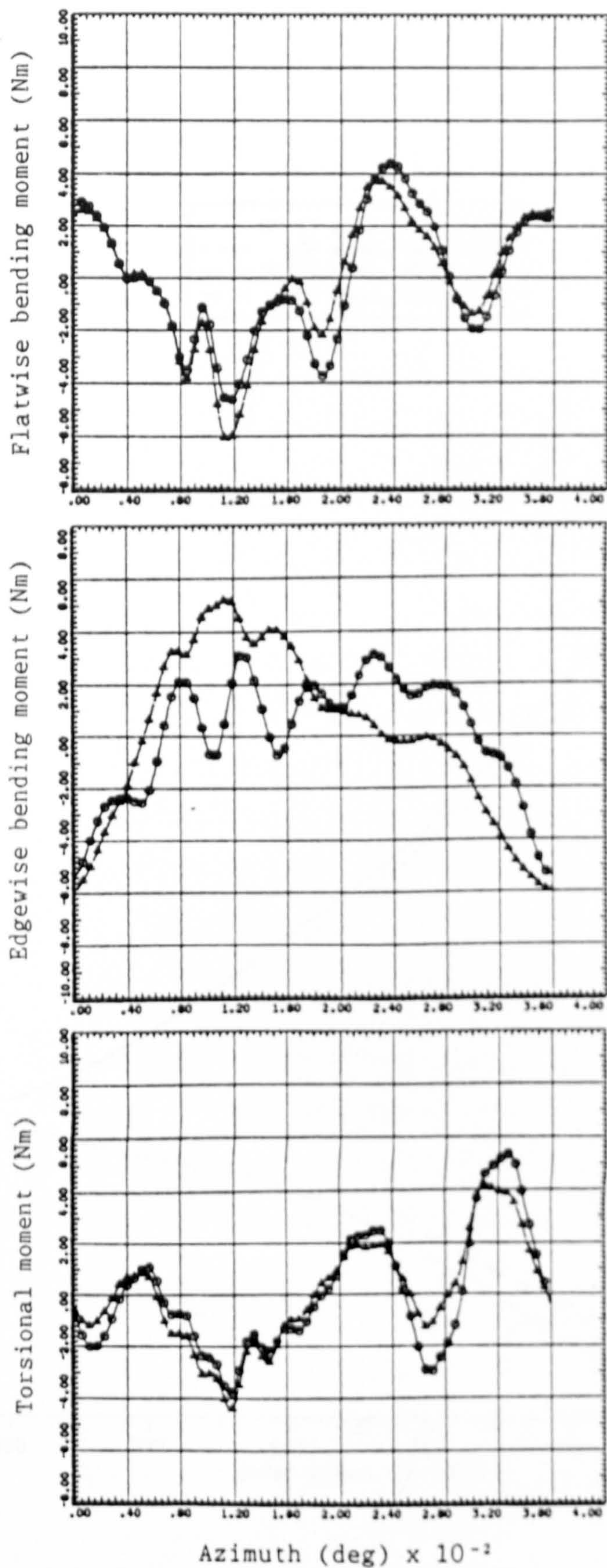


Fig 77 Preliminary design showing swept shear centre



—○— Mk2 swept tip blade

—△— Mk3 swept tip blade

Fig 78 Predicted (J146) root bending and torsional moments for a preliminary Mk3 blade design, thrust = 900N, $\mu = 0.34$

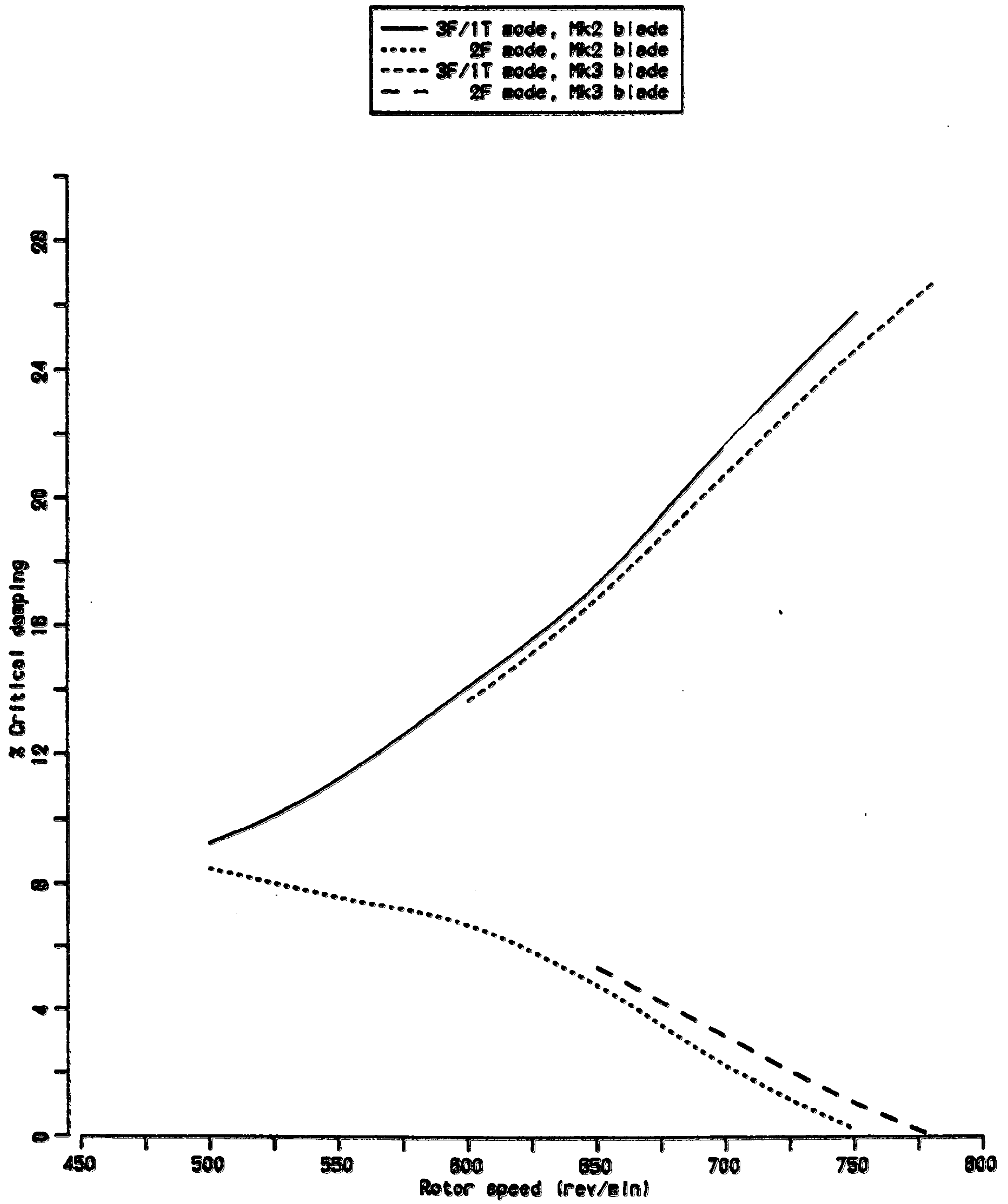
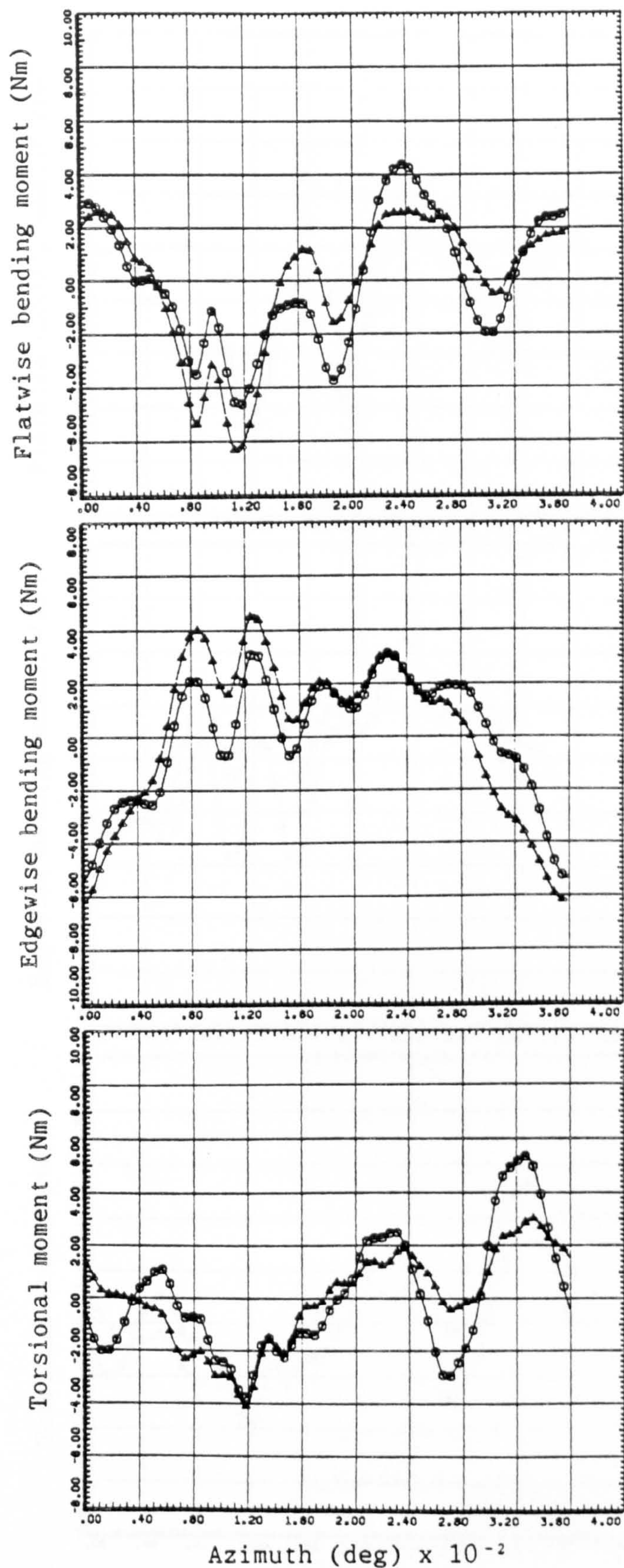


Fig 79 Modal damping for a preliminary Mk3 blade design



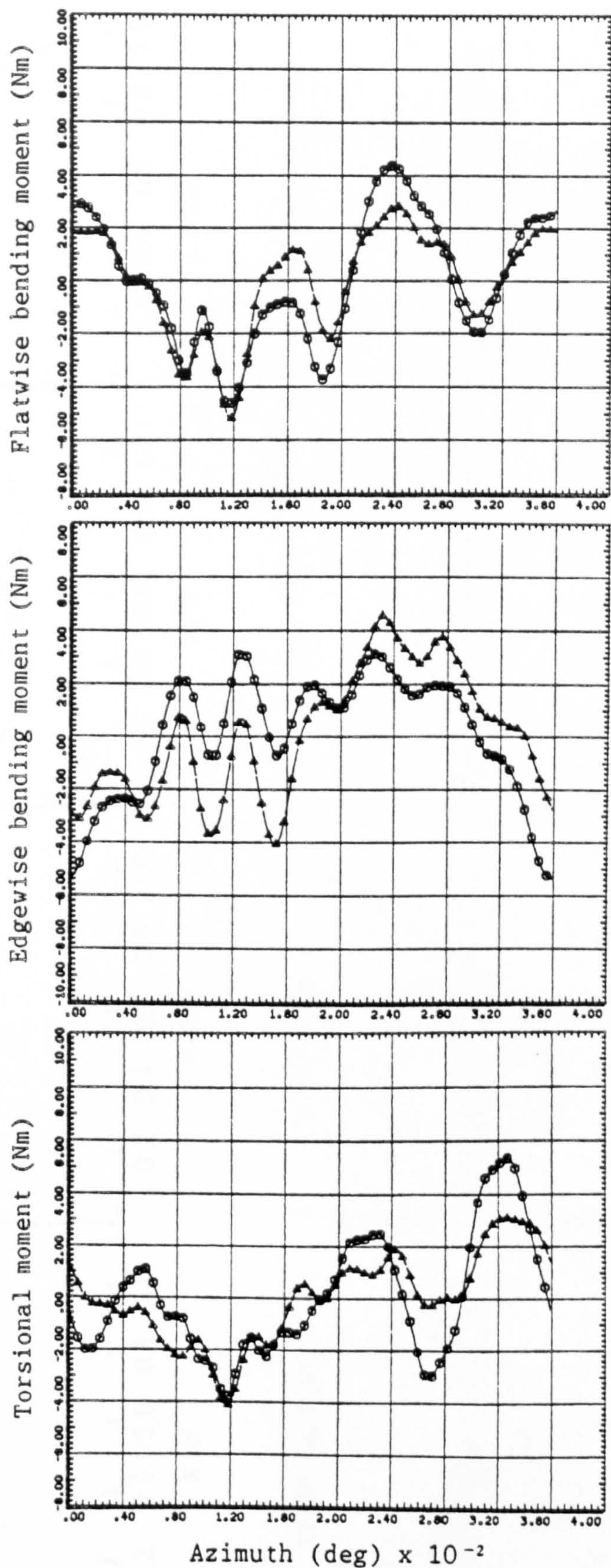
—○—

Mk2 swept tip blade

—△—

Mk3 swept tip blade

Fig 80 Predicted (J146) root bending and torsional moments for a second Mk3 blade design, thrust = 900N, $\mu = 0.34$



—○— Mk2 swept tip blade

—△— Mk3 swept tip blade

Fig 81 Predicted (J146) root bending and torsional moments for the final Mk3 blade design. thrust = 900N. $u = 0.34$

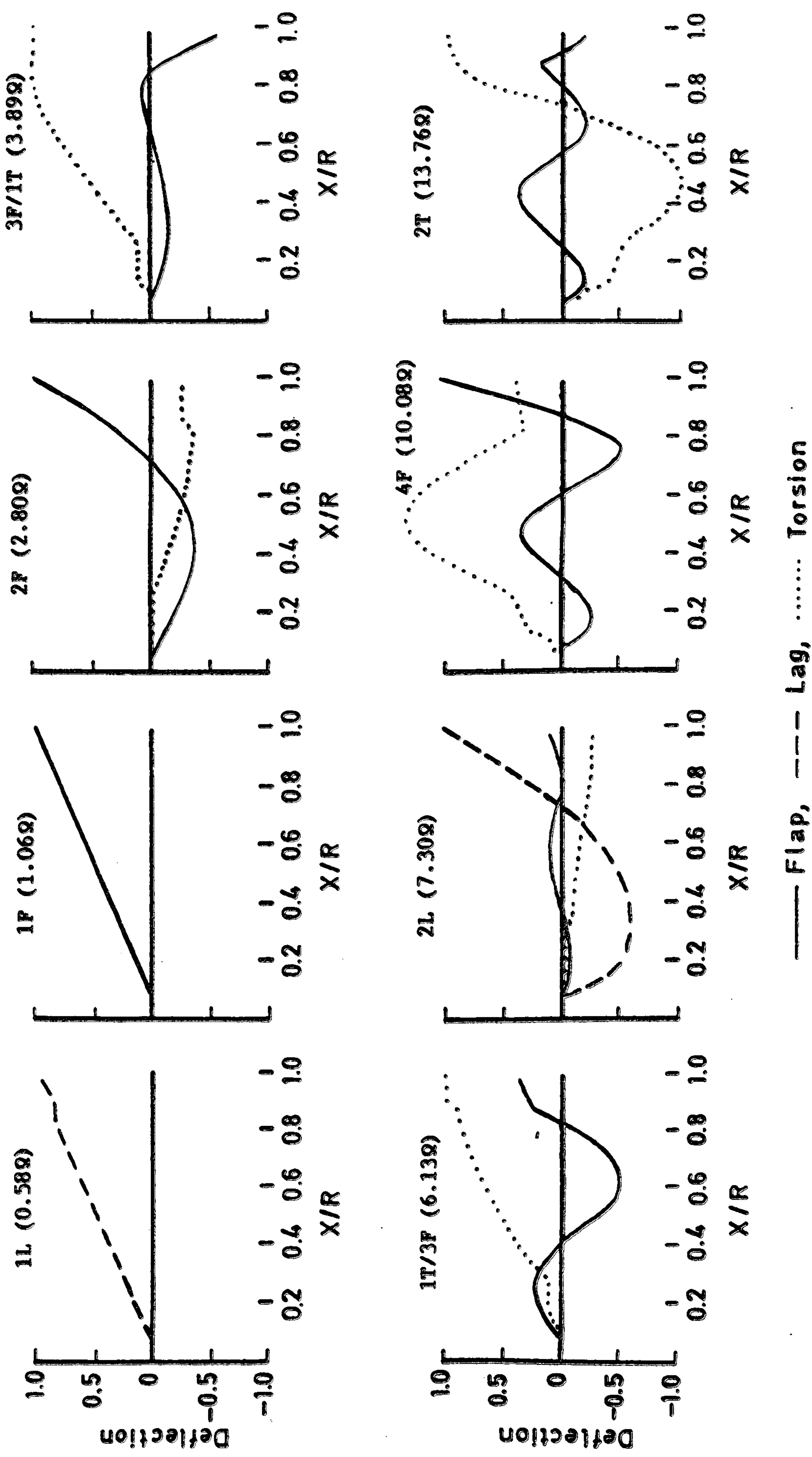


Fig 82 J145 mode shapes for the Mk3 swept tip blade (600 rev/min)

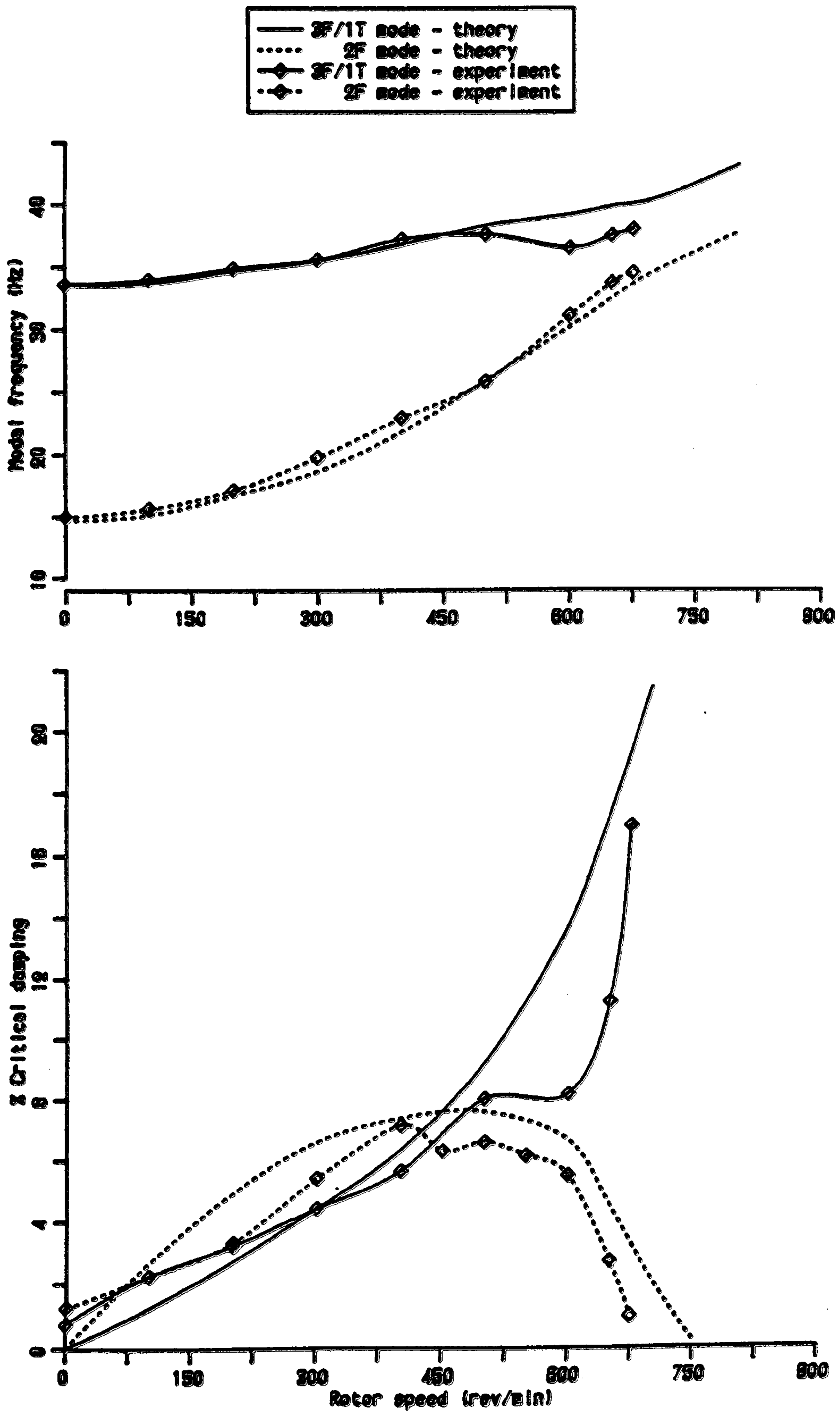


Fig 83 Experimental and theoretical modal frequencies and dampings for the Mk2 swept tip blade

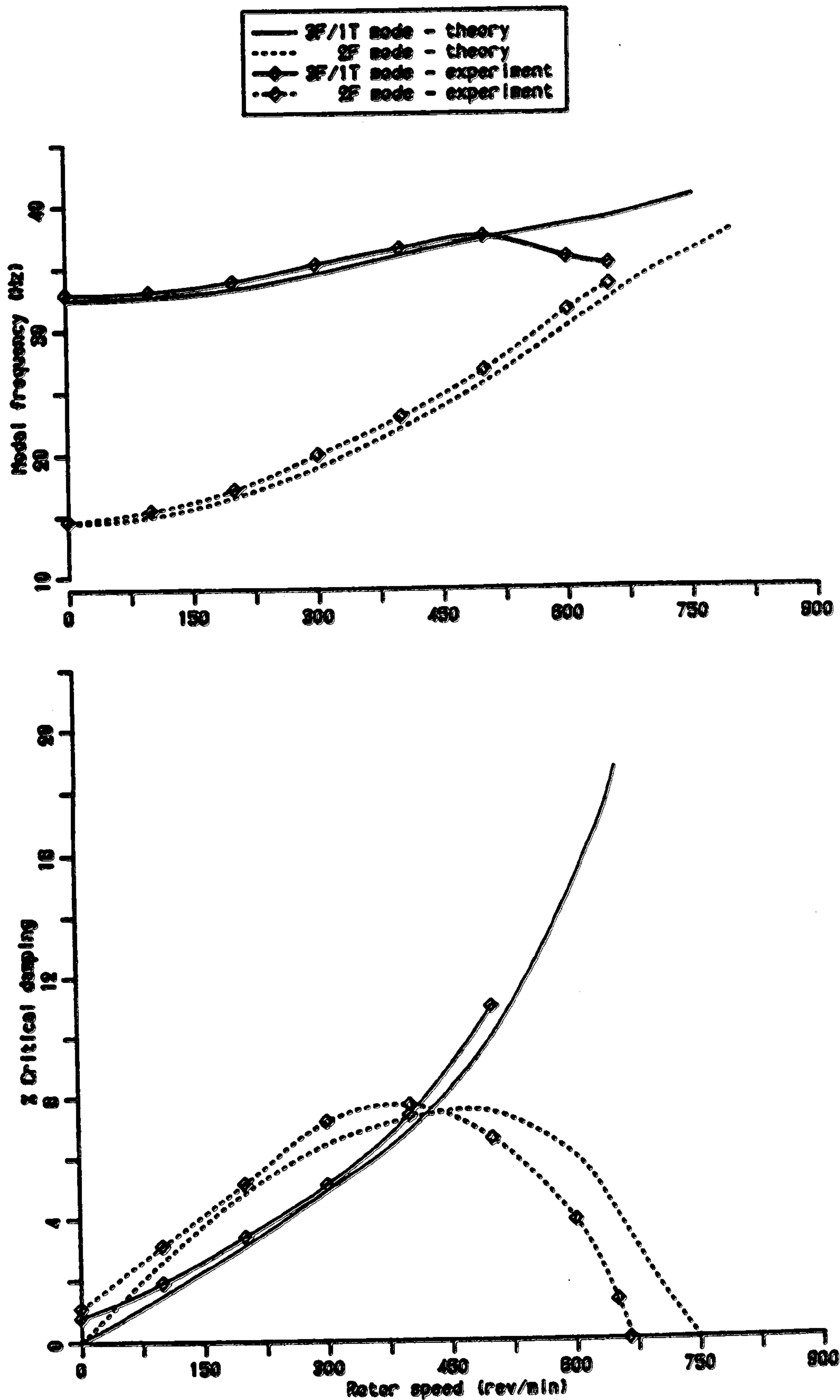
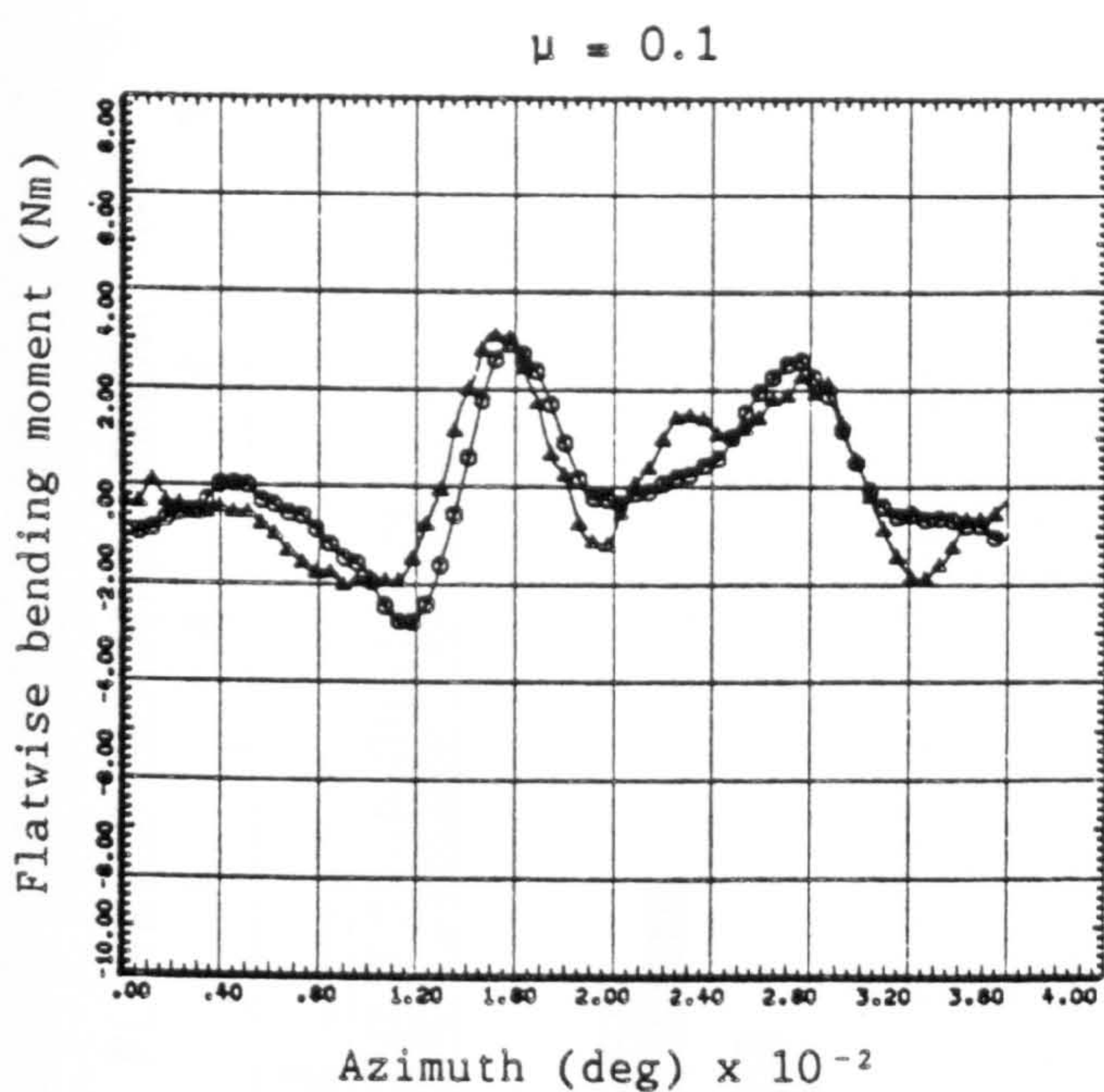
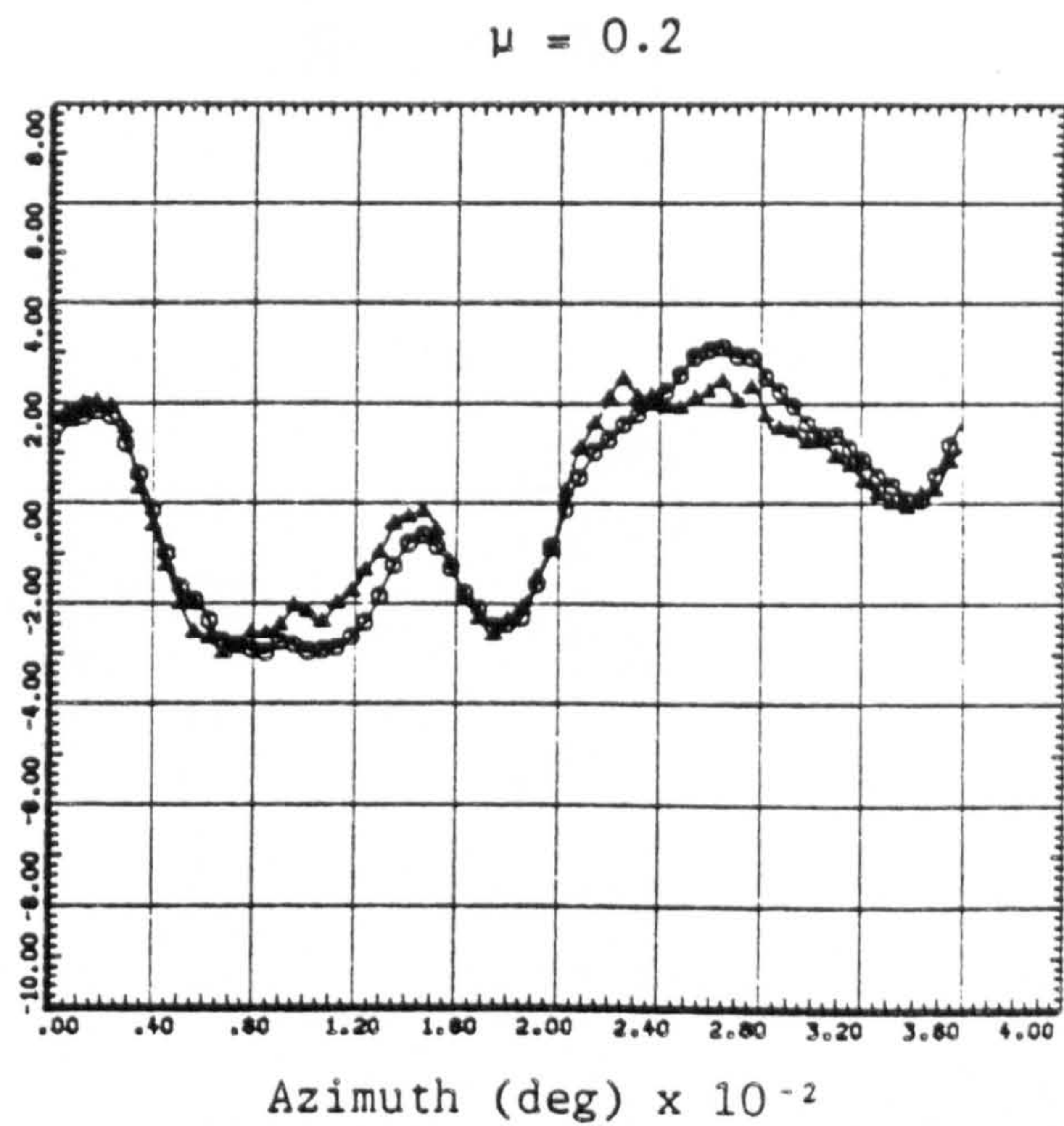
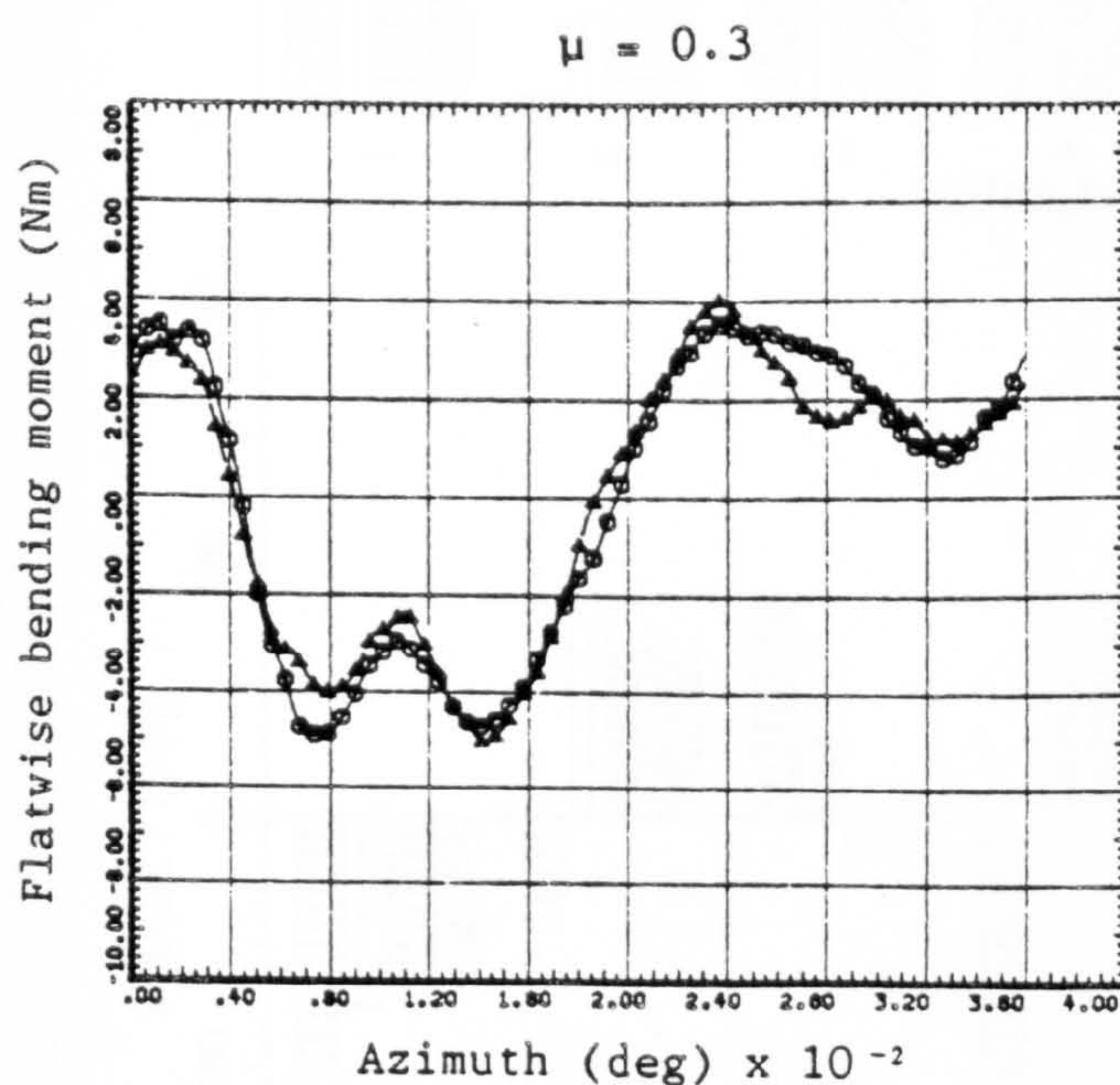
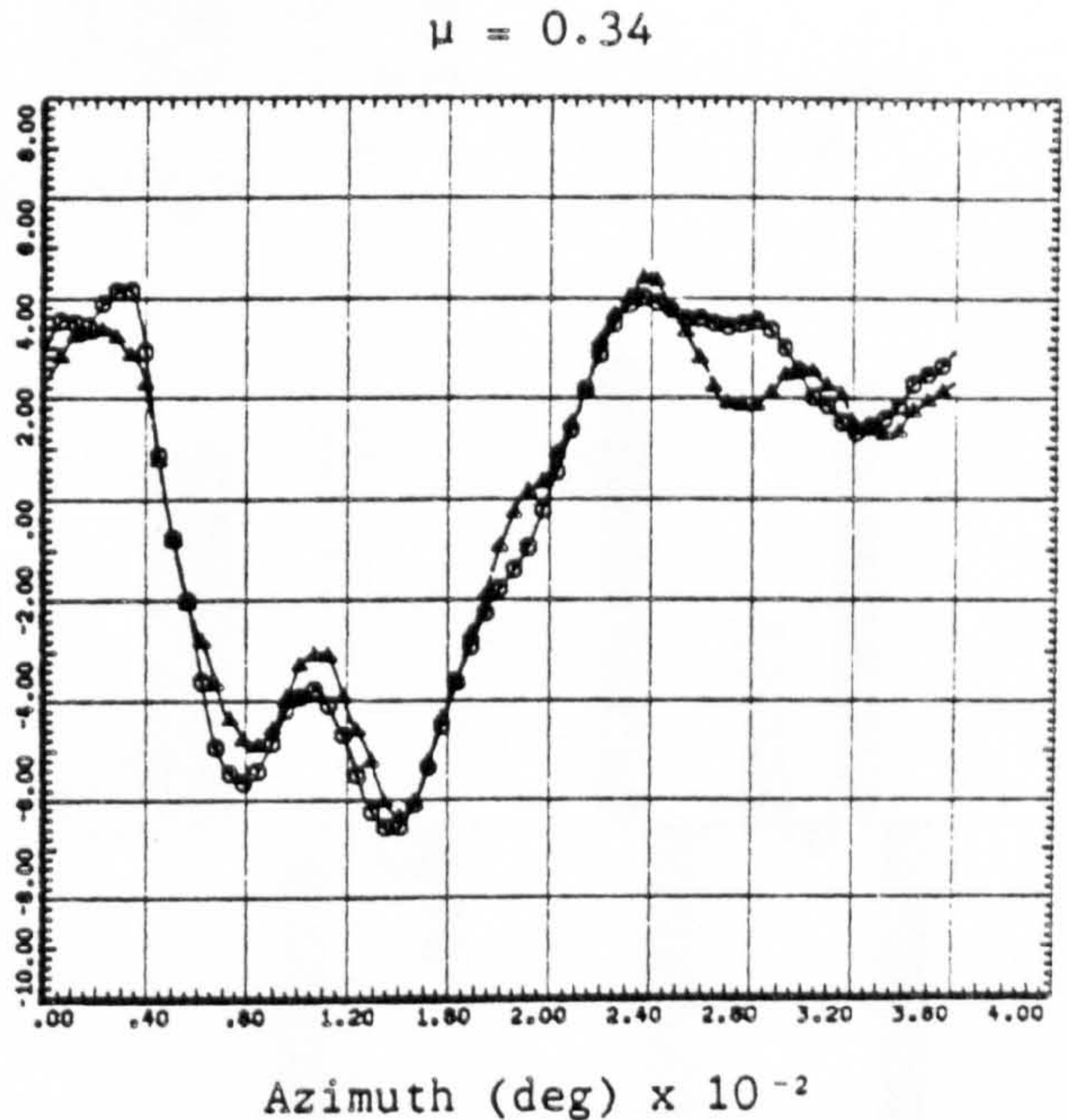


Fig 84 Experimental and theoretical modal frequencies and dampings for the Mk3 swept tip blade



—○— Mk2 swept tip blade

—△— Mk3 swept tip blade

Fig 85 Measured flatwise bending moments on the Mk2 and Mk3 swept tip blades, thrust = 900N, 30% R

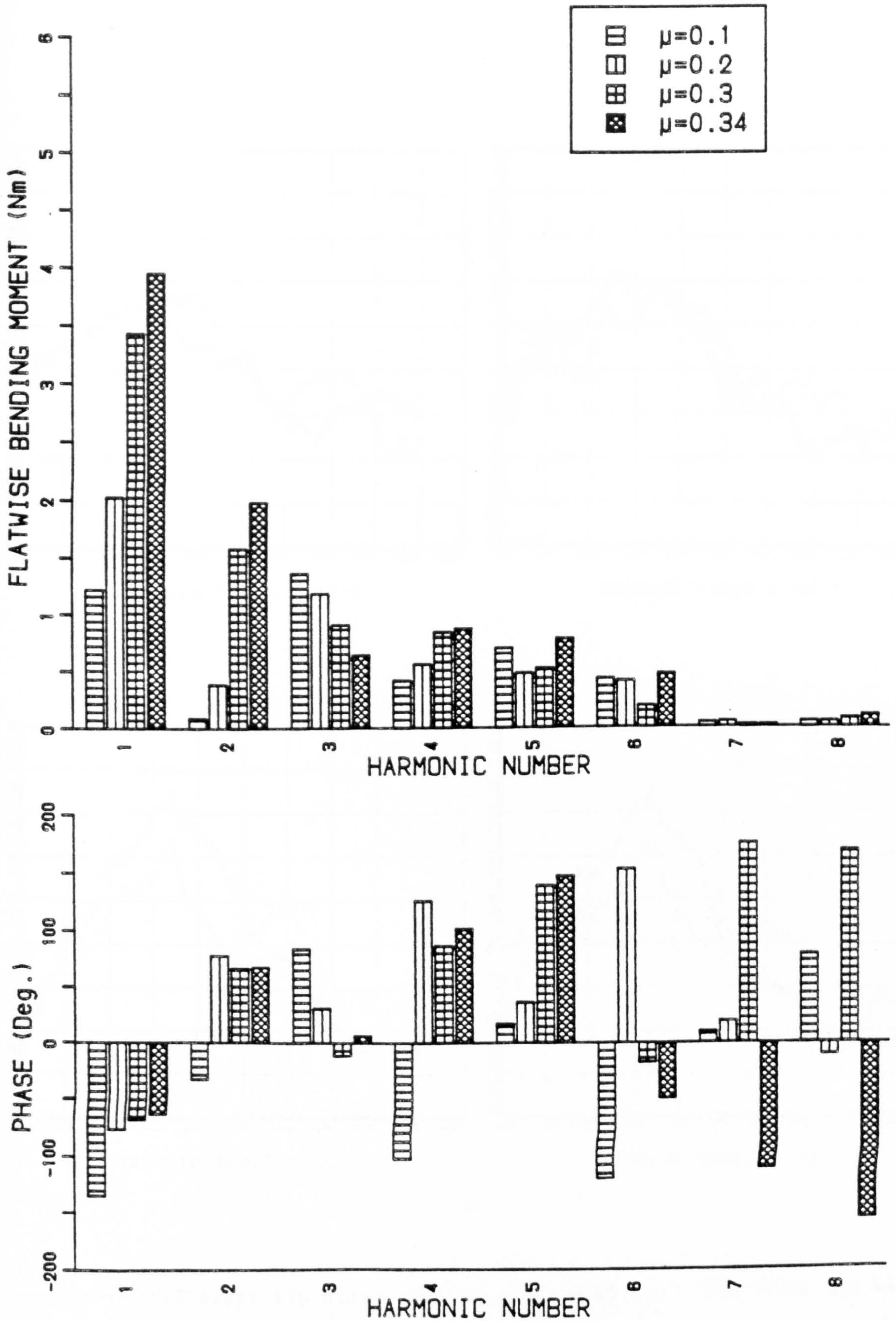


Fig 86 Harmonic analysis of the measured flatwise bending moments on the Mk3 swept tip blade, thrust = 900N, 30% R

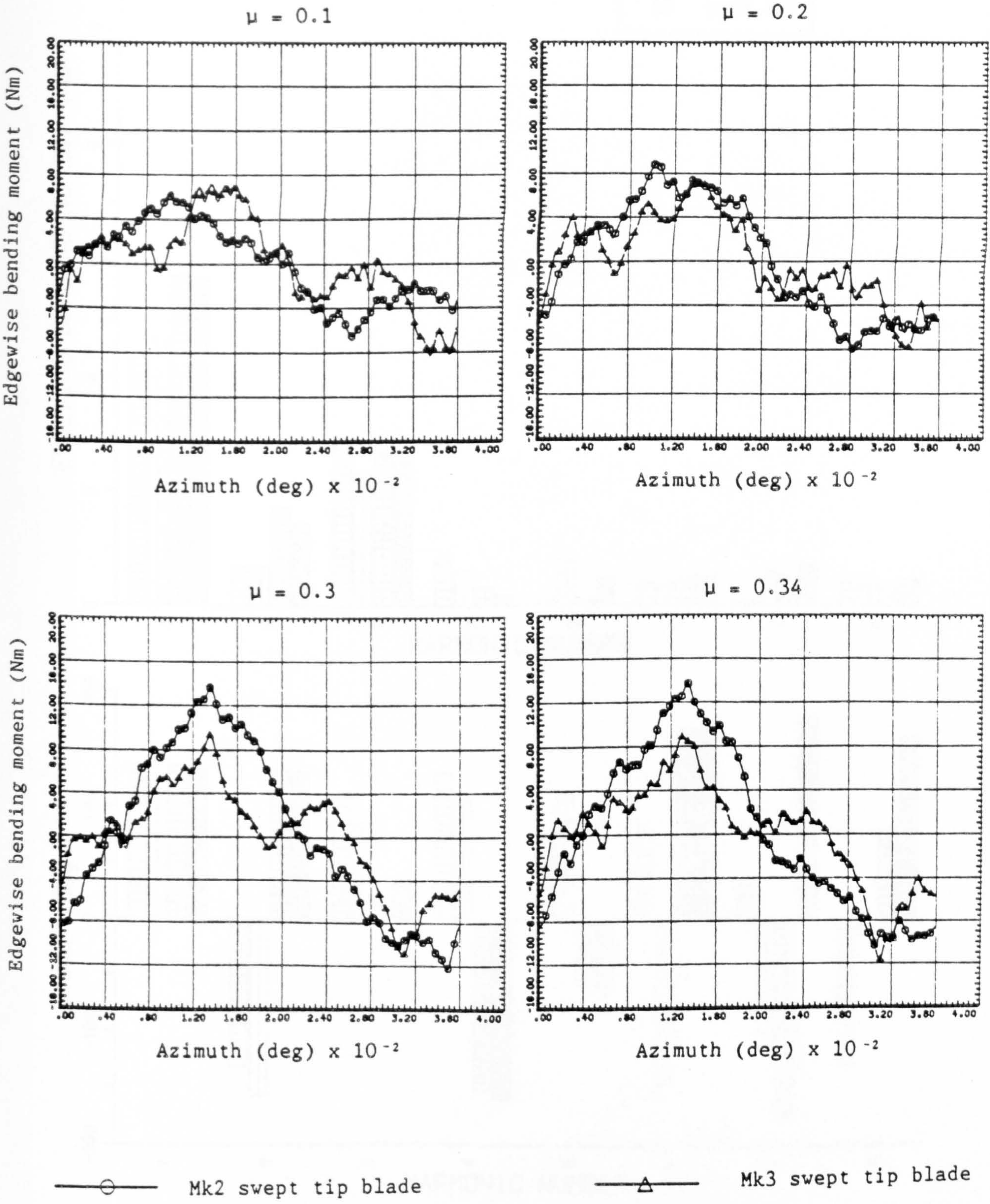


Fig 87 Measured edgewise bending moments on the Mk2 and Mk3 swept tip blades, thrust = 900N, 37% R

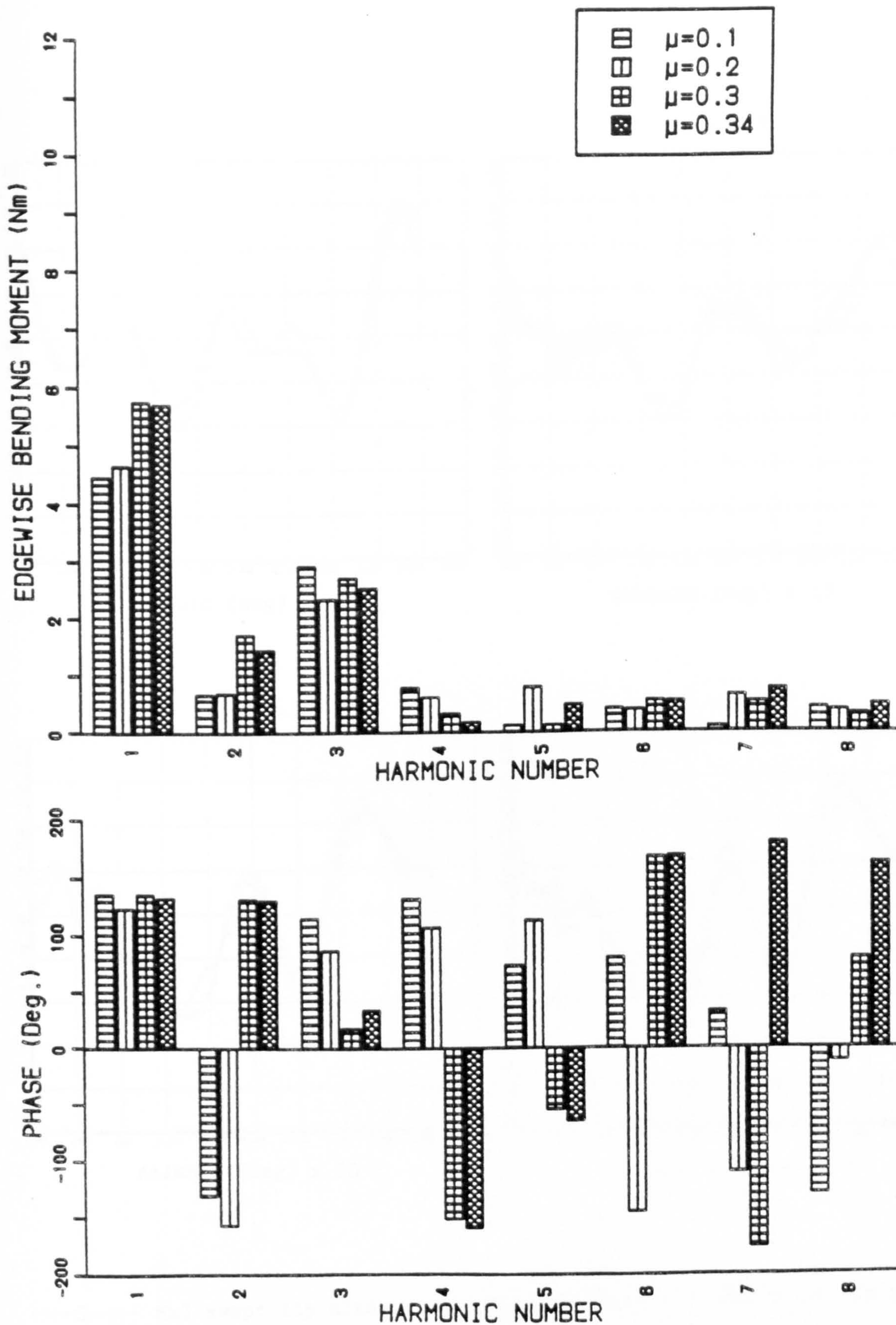


Fig 88 Harmonic analysis of the measured edgewise bending moments on the Mk3 swept tip blade, thrust = 900N, 37% R

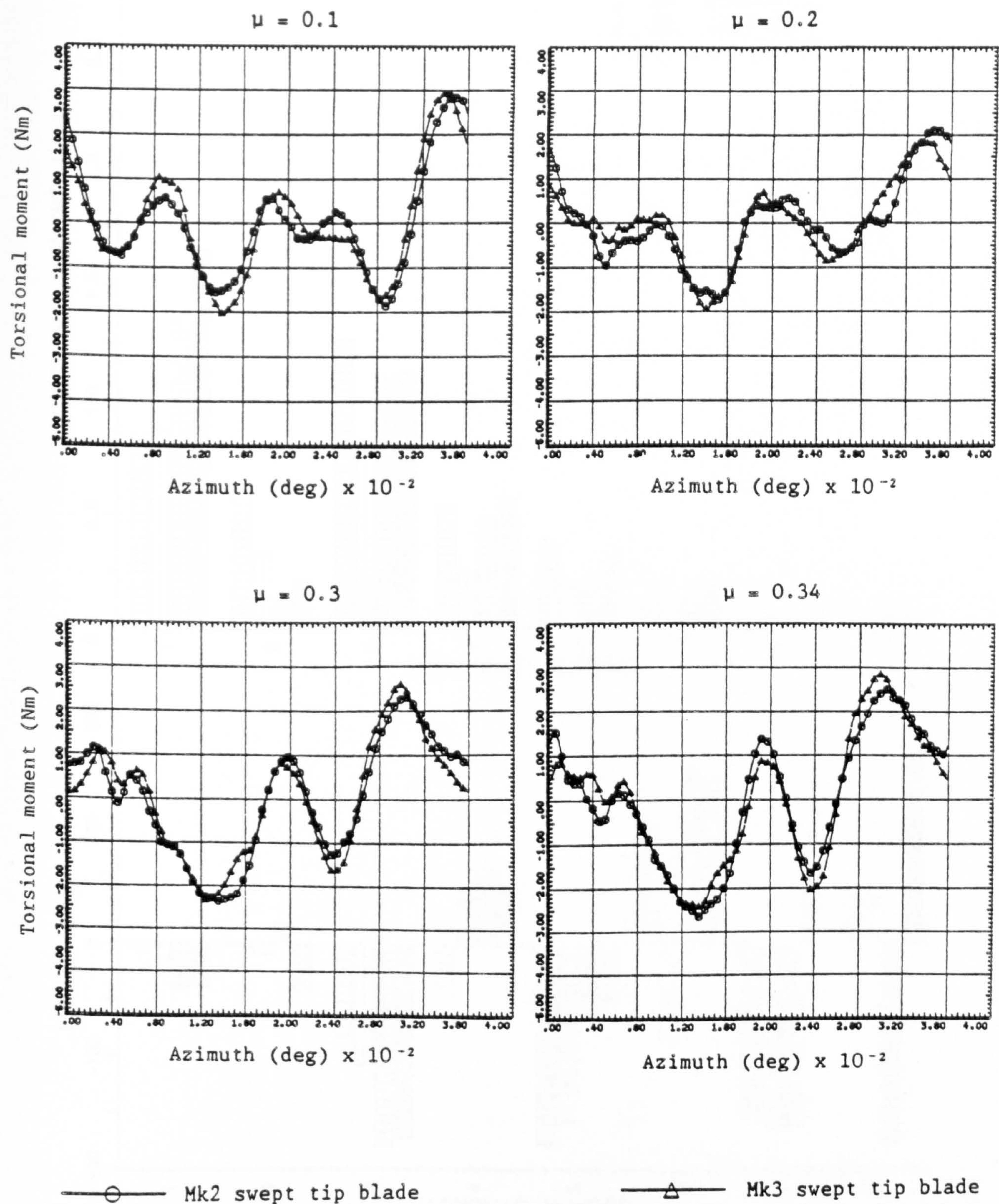


Fig 89 Measured torsional moments on the Mk2 and Mk3 swept tip blades, thrust = 900N, 30% R

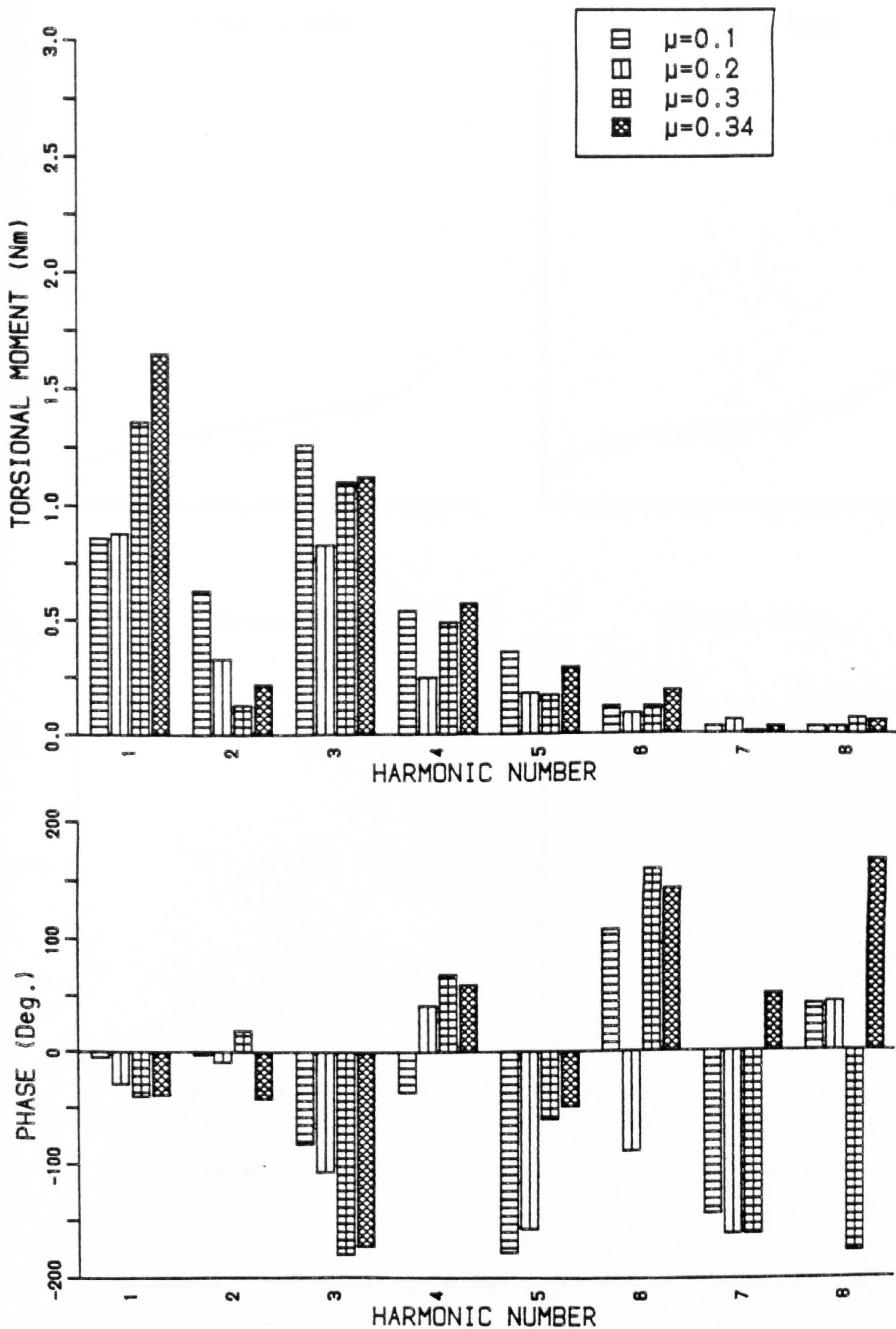


Fig 90 Harmonic analysis of the measured torsional moments on the Mk3 swept tip blade, thrust = 900N, 30% R

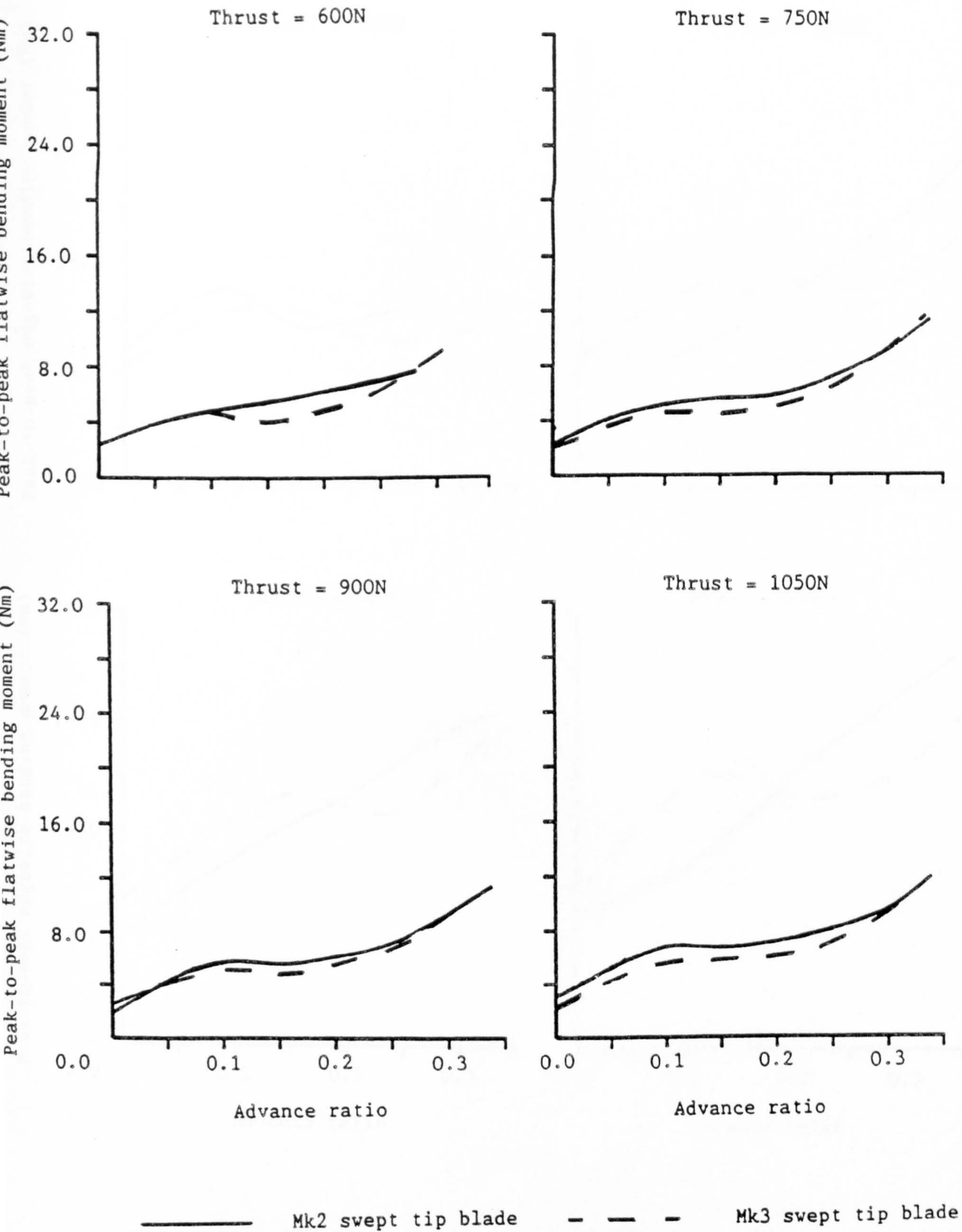
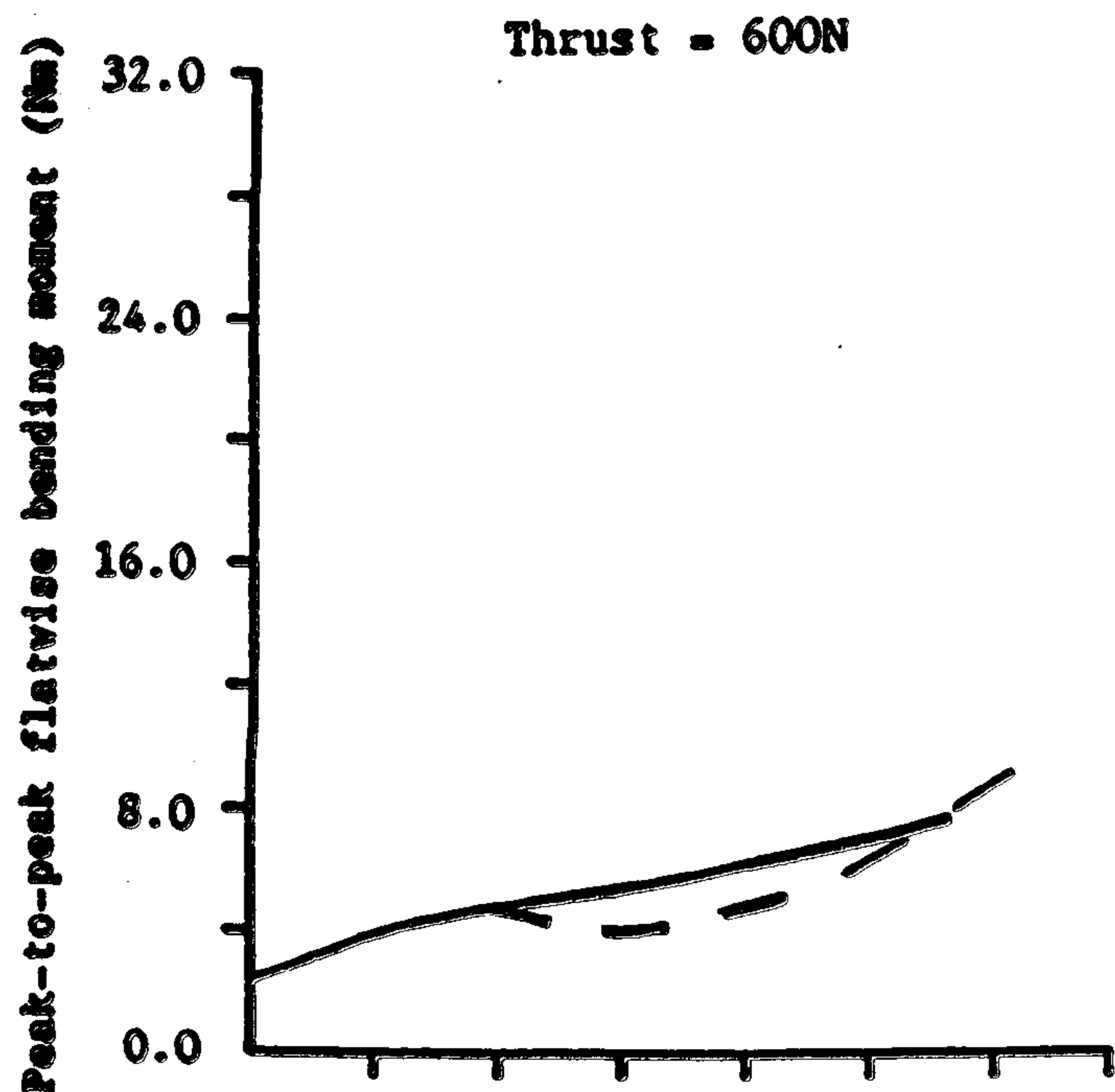
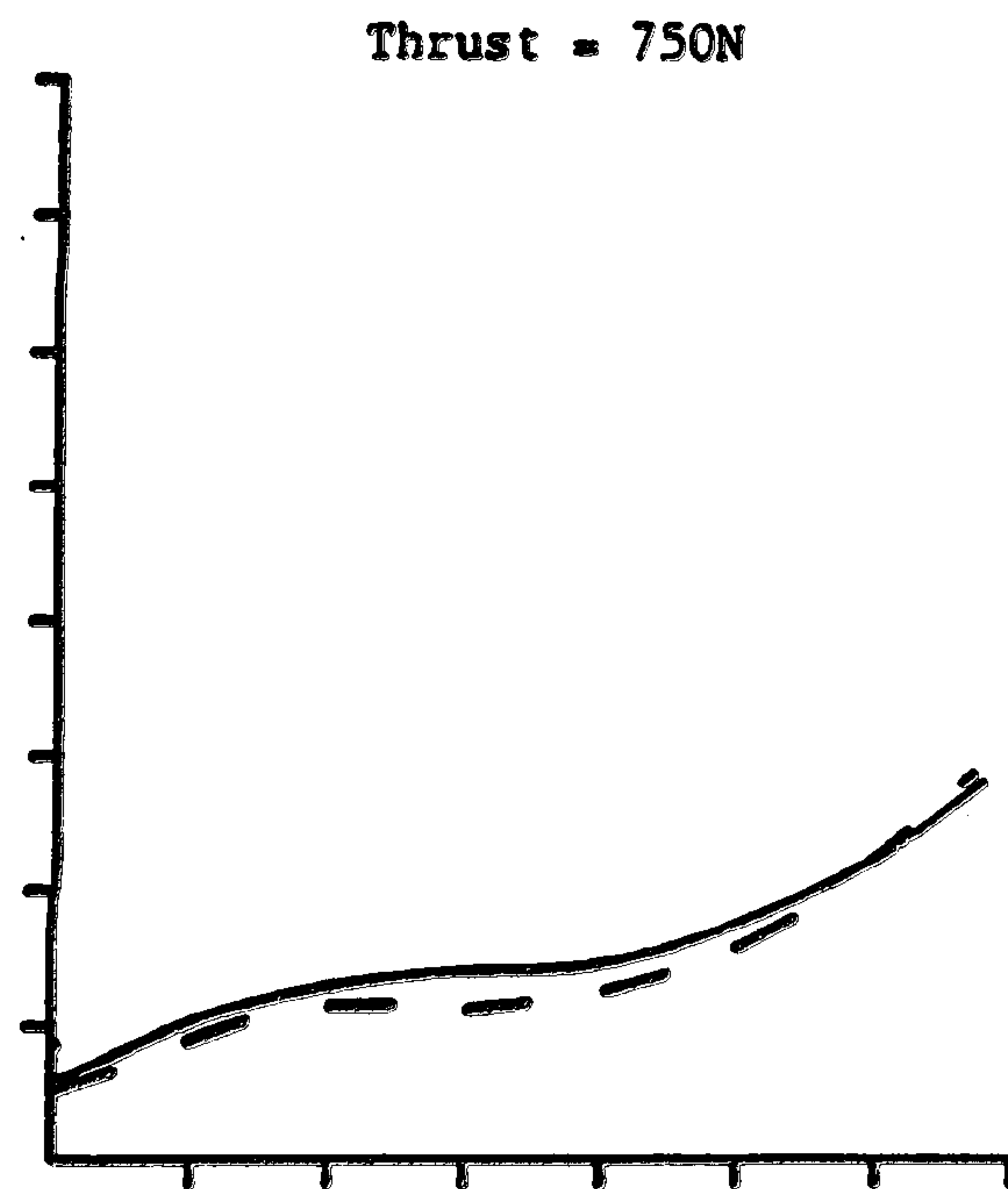
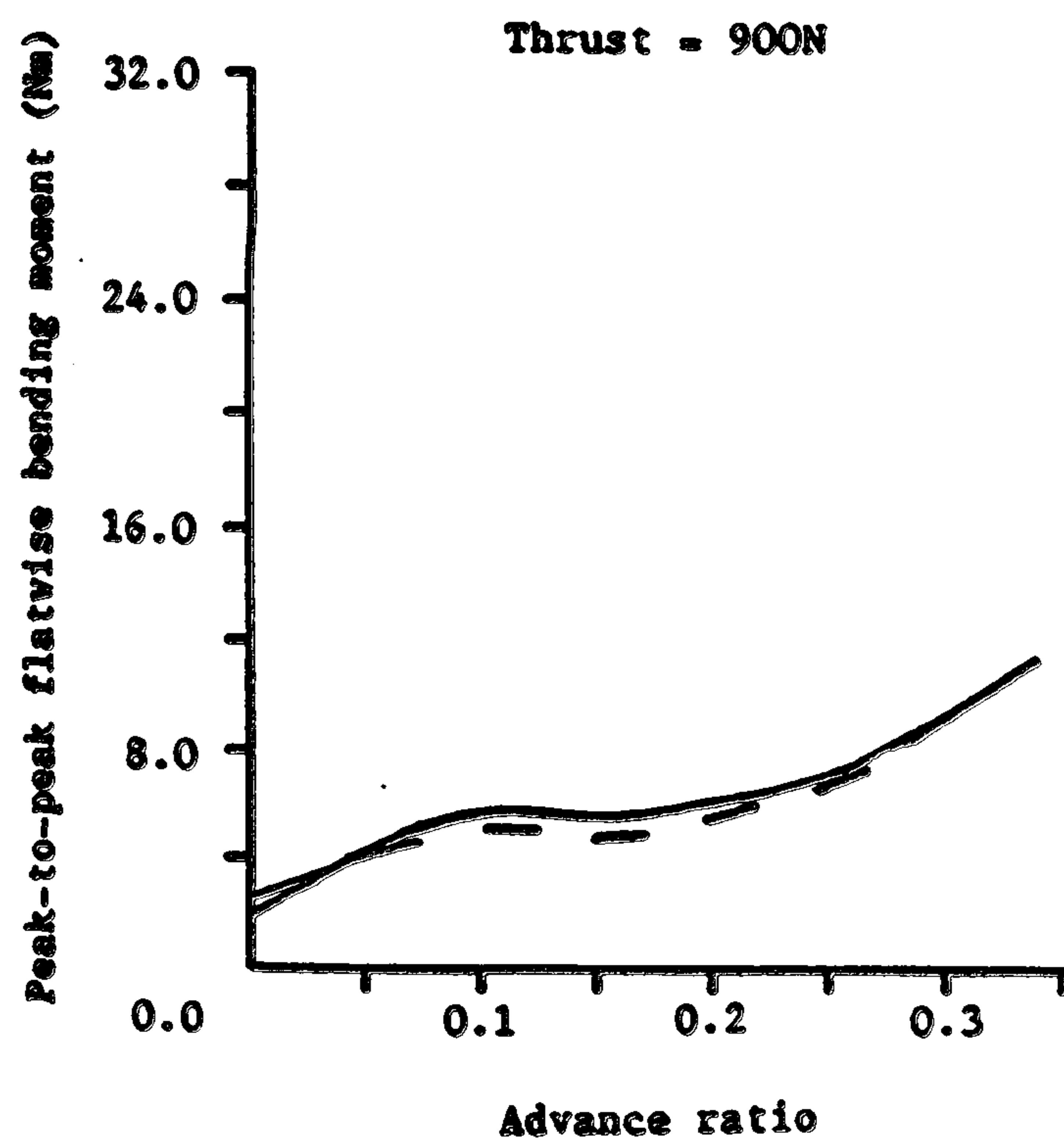
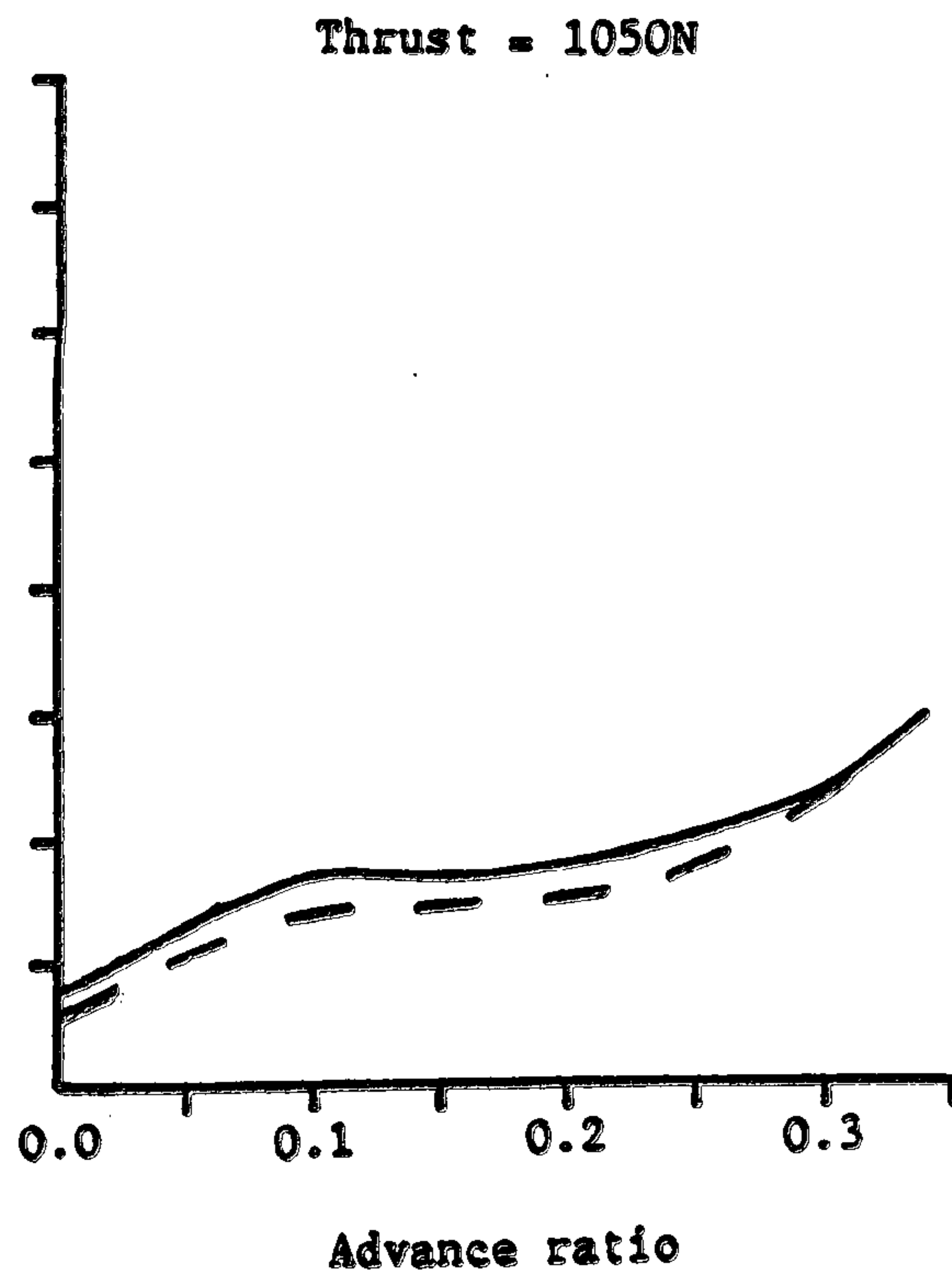


Fig 91 Measured peak-to-peak flatwise bending moments - 30% R



———— Mk2 swept tip blade - - - Mk3 swept tip blade

Fig 91 Measured peak-to-peak flatwise bending moments - 30% R

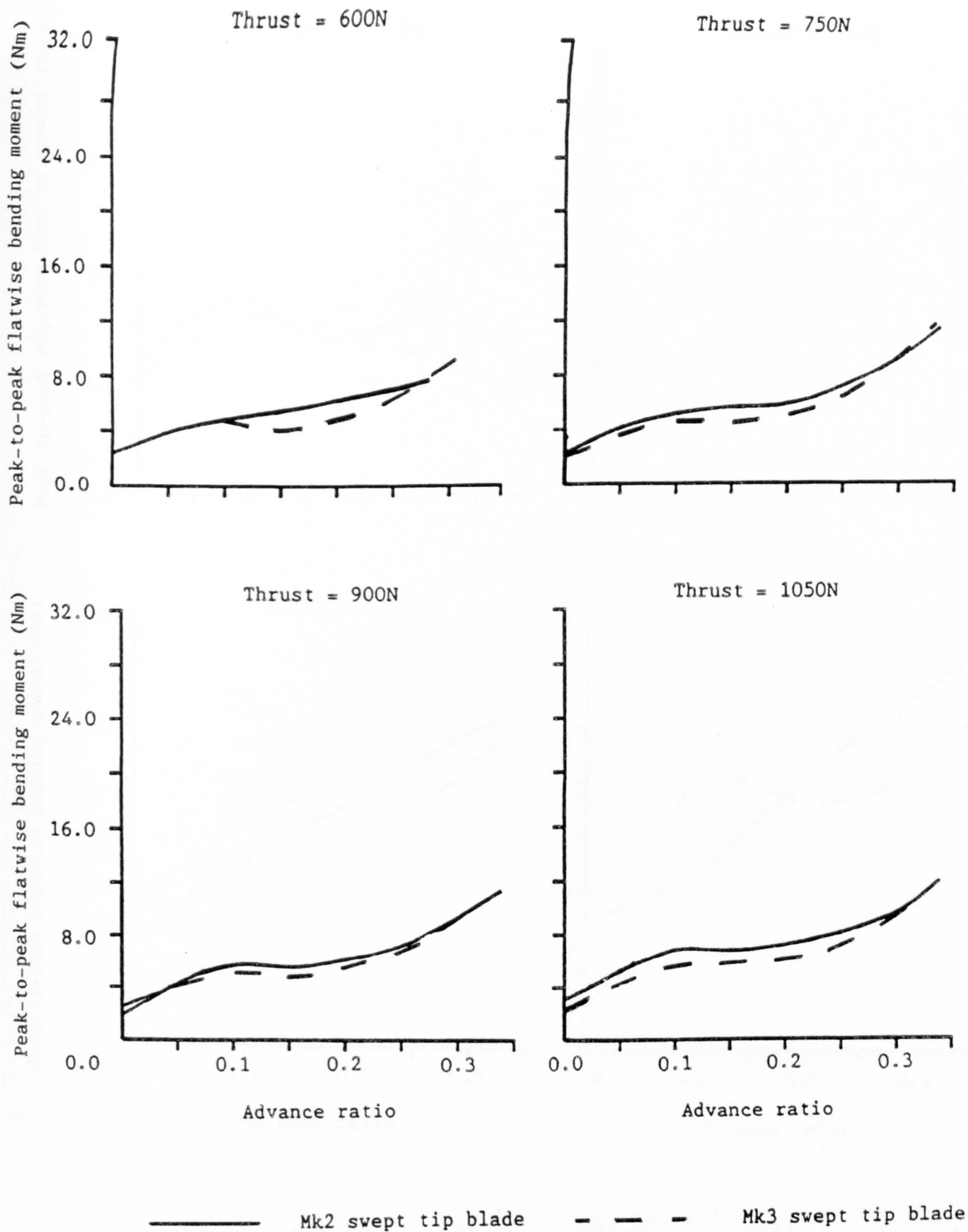
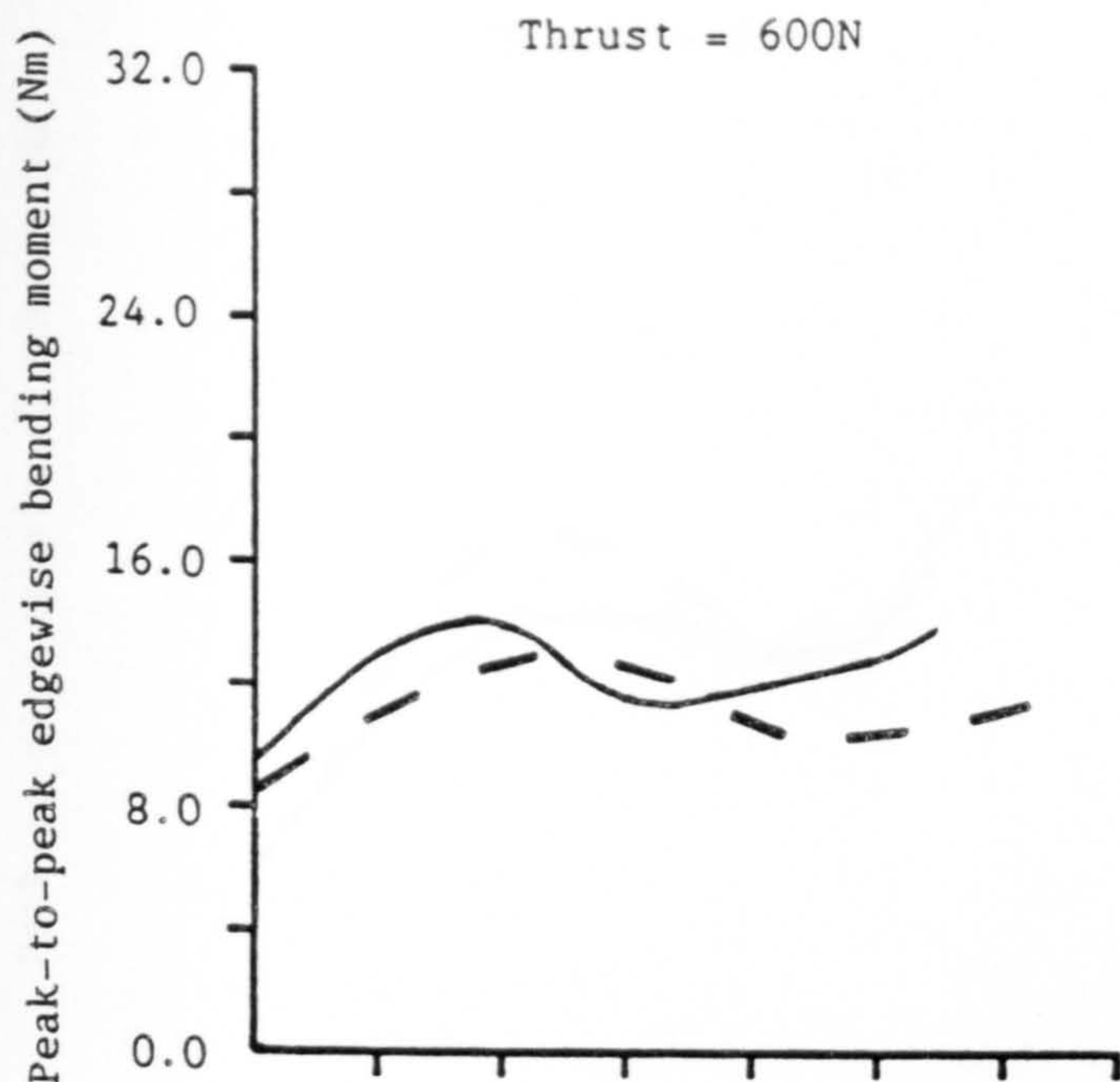
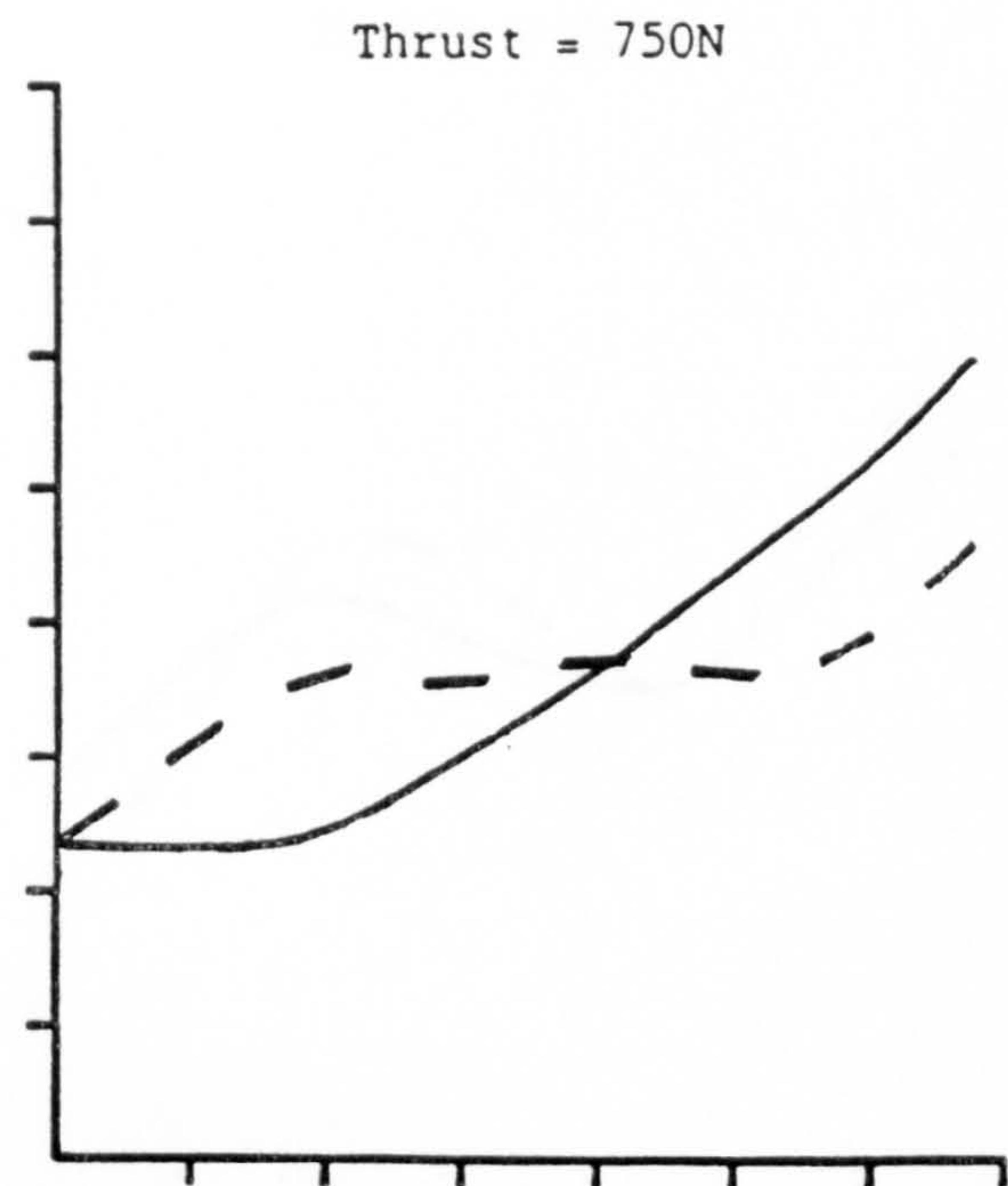
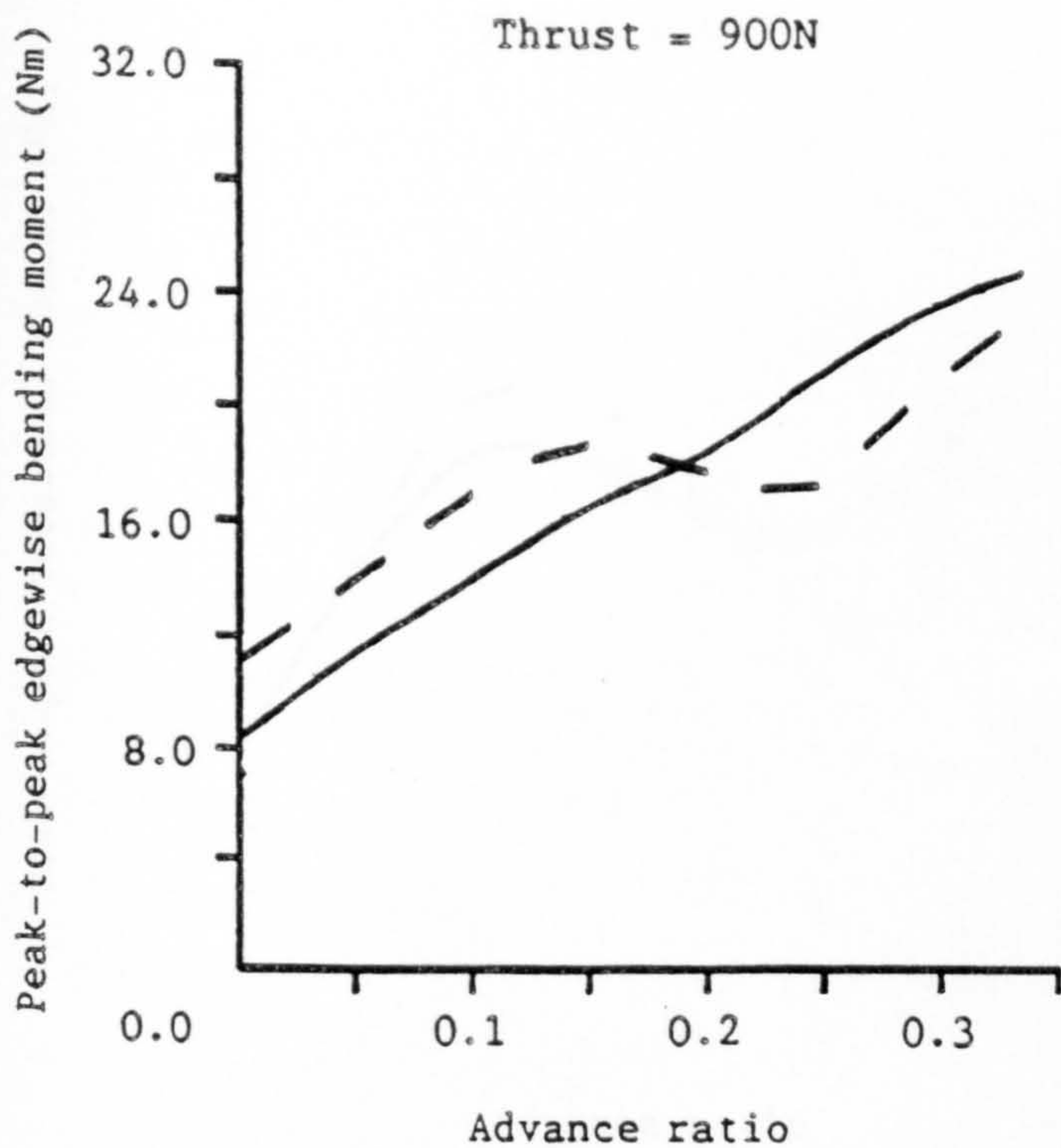
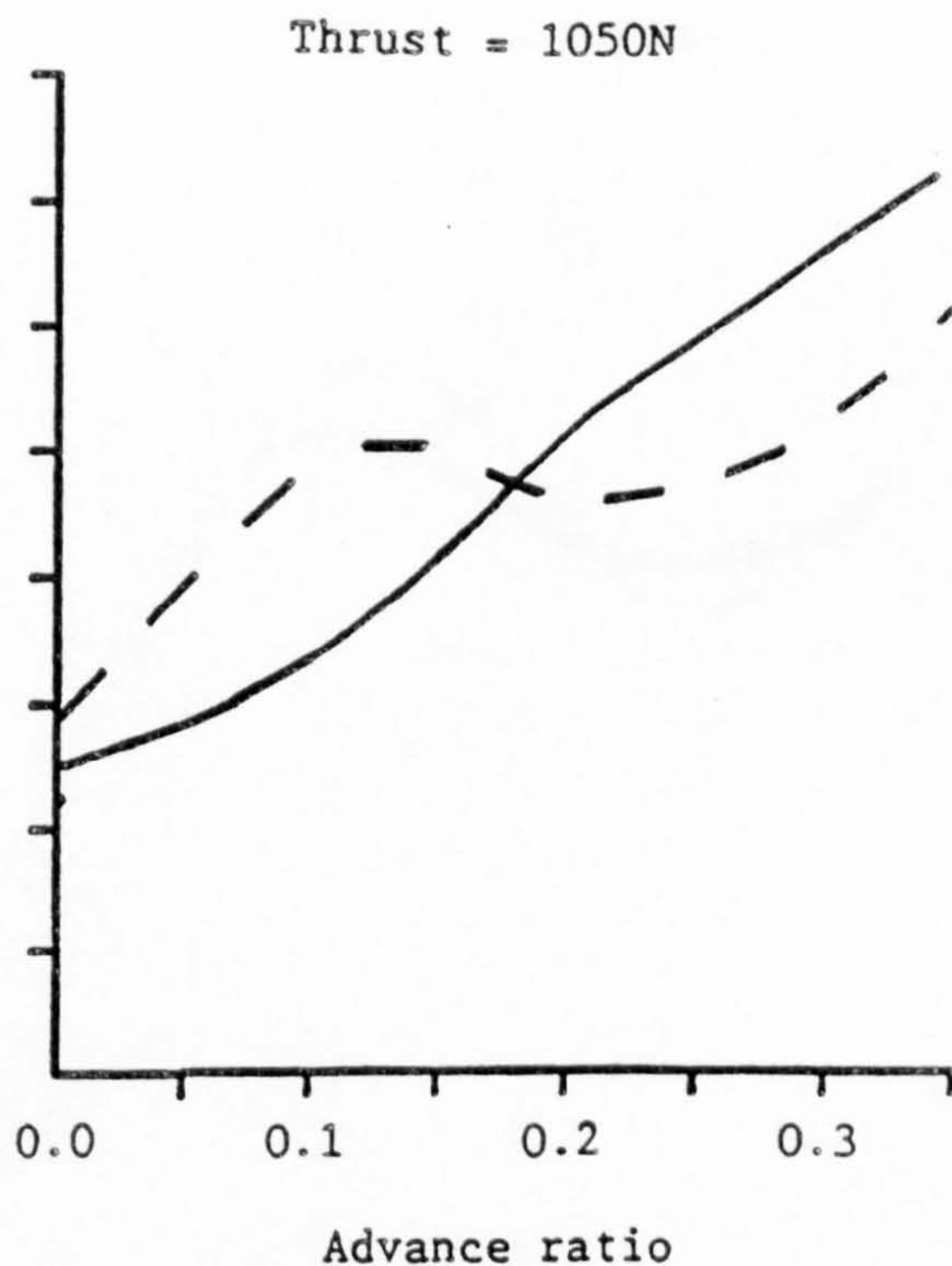


Fig 91 Measured peak-to-peak flatwise bending moments - 30% R



———— Mk2 swept tip blade

- - - - - Mk3 swept tip blade

Fig 92 Measured peak-to-peak edgewise bending moments - 37% R

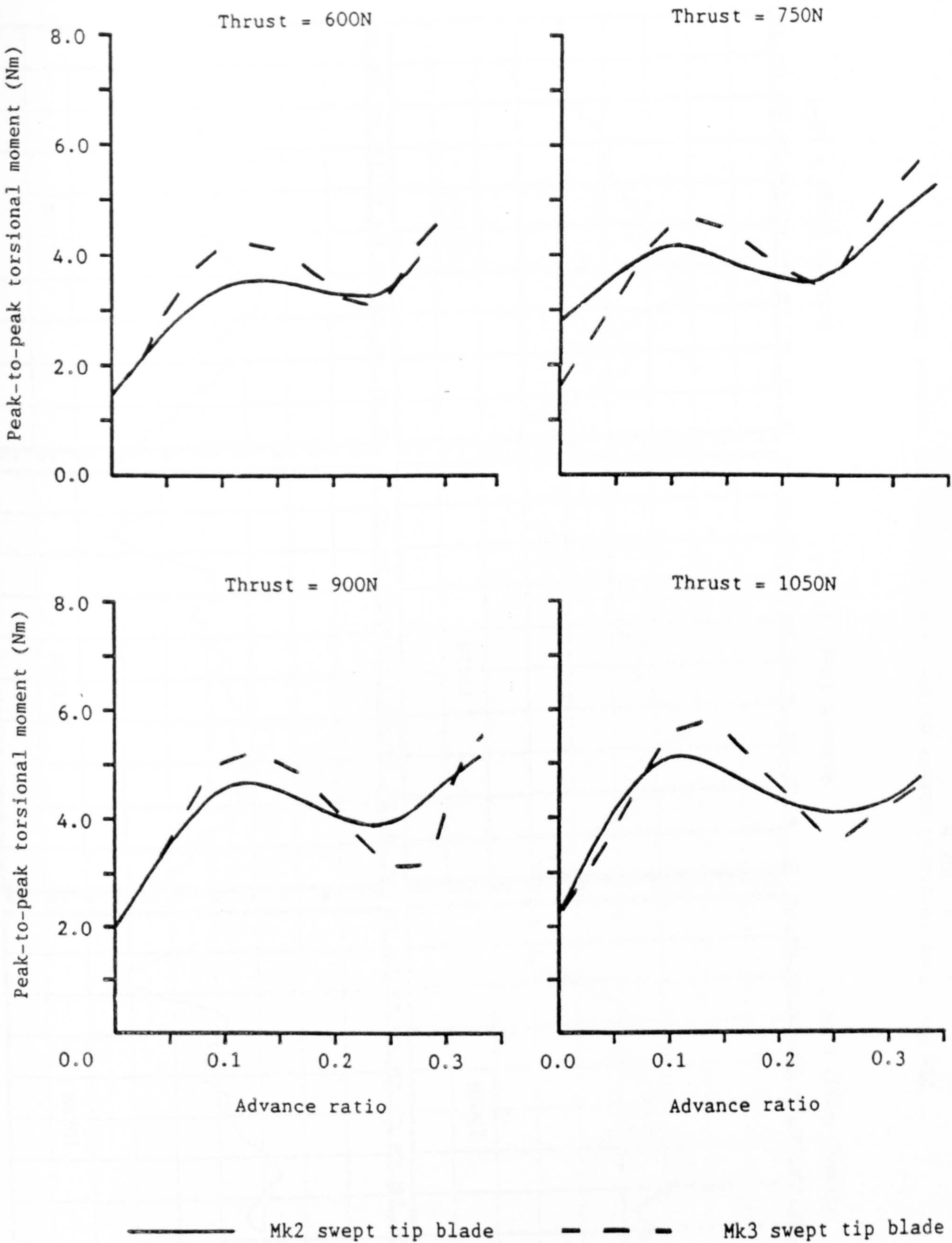


Fig 93 Measured peak-to-peak torsional moments - 30% R

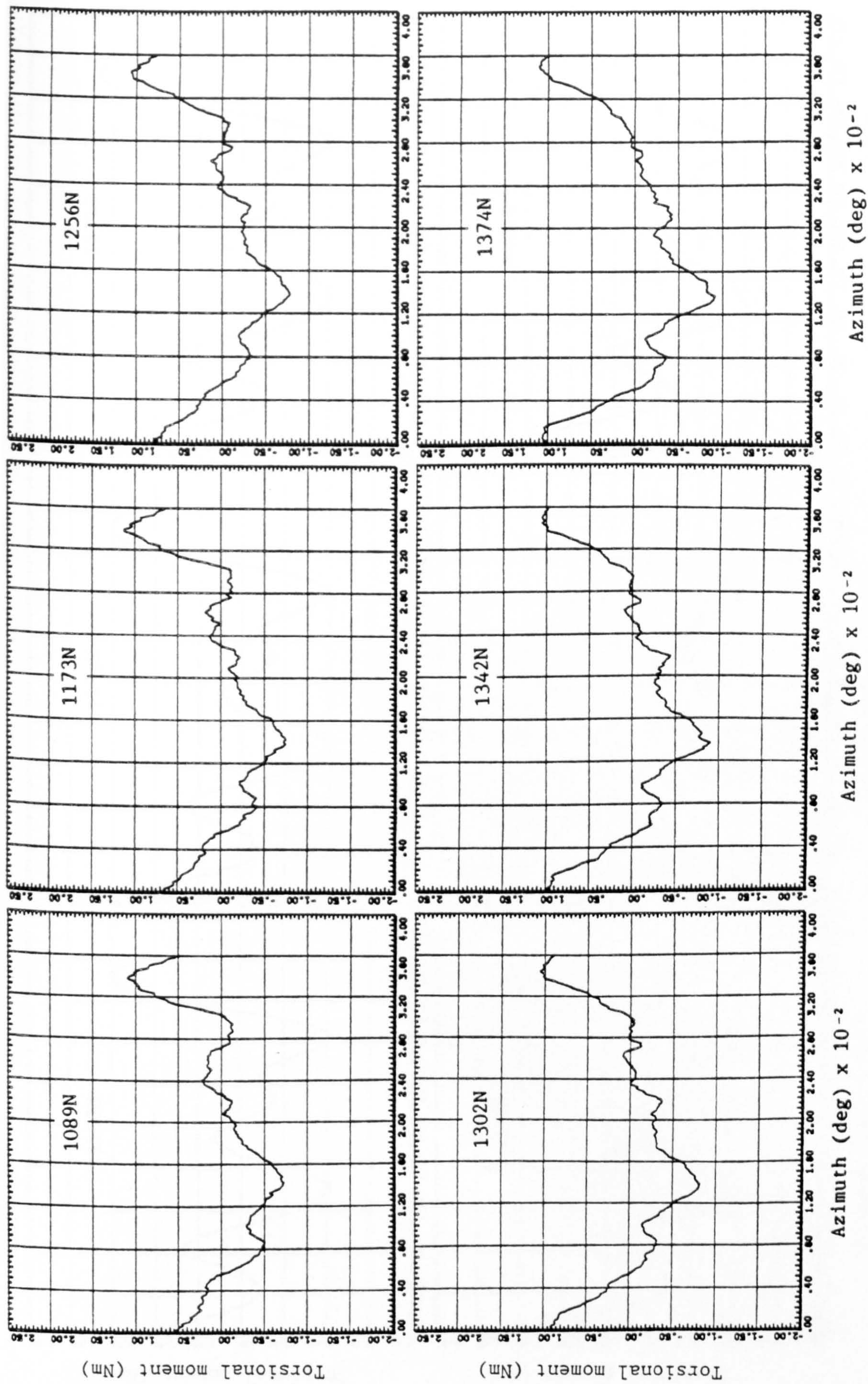


Fig 94 Root torsional moment on the baseline blade for various thrusts,
 $\mu = 0.1, 30\% R$

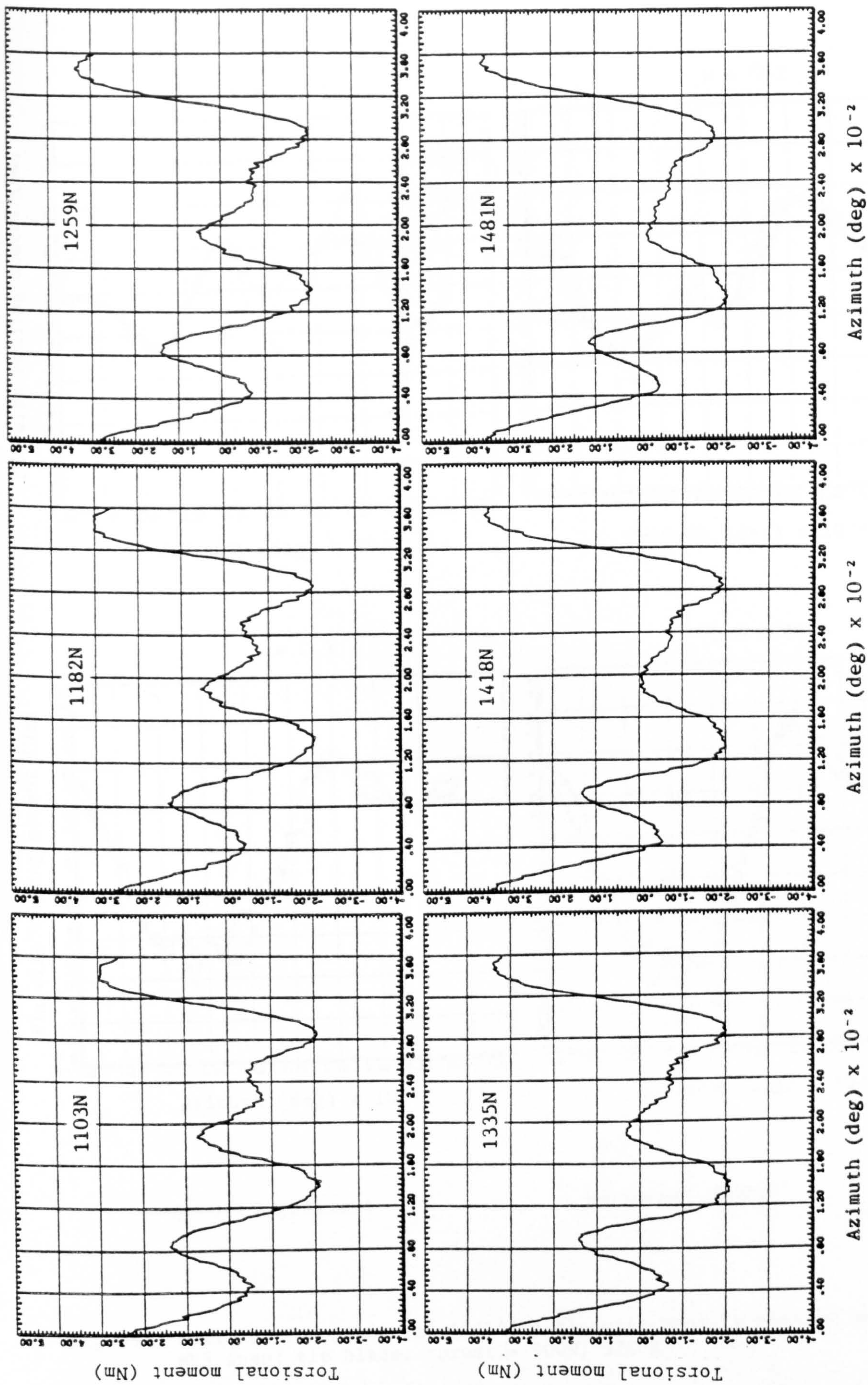
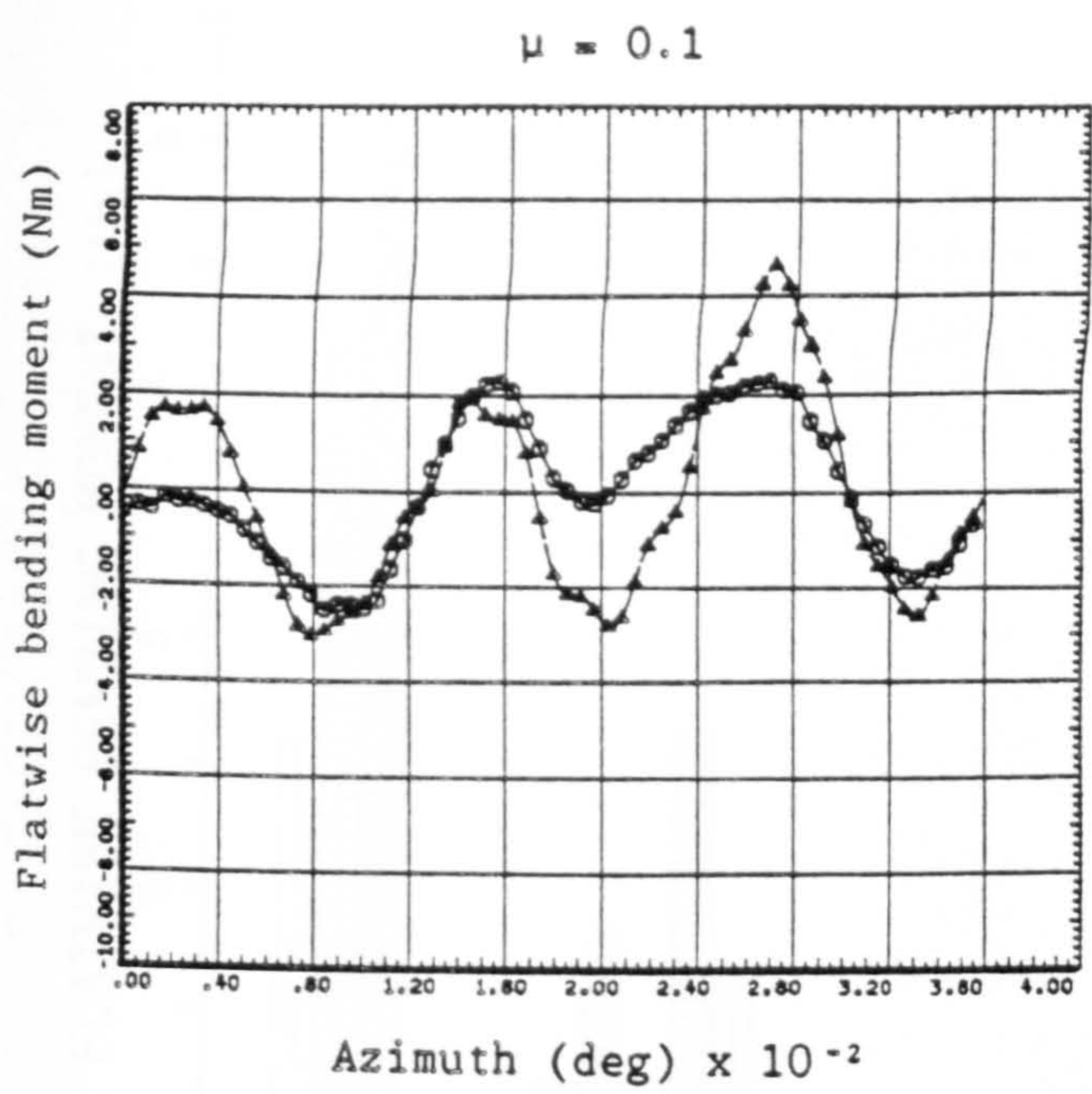
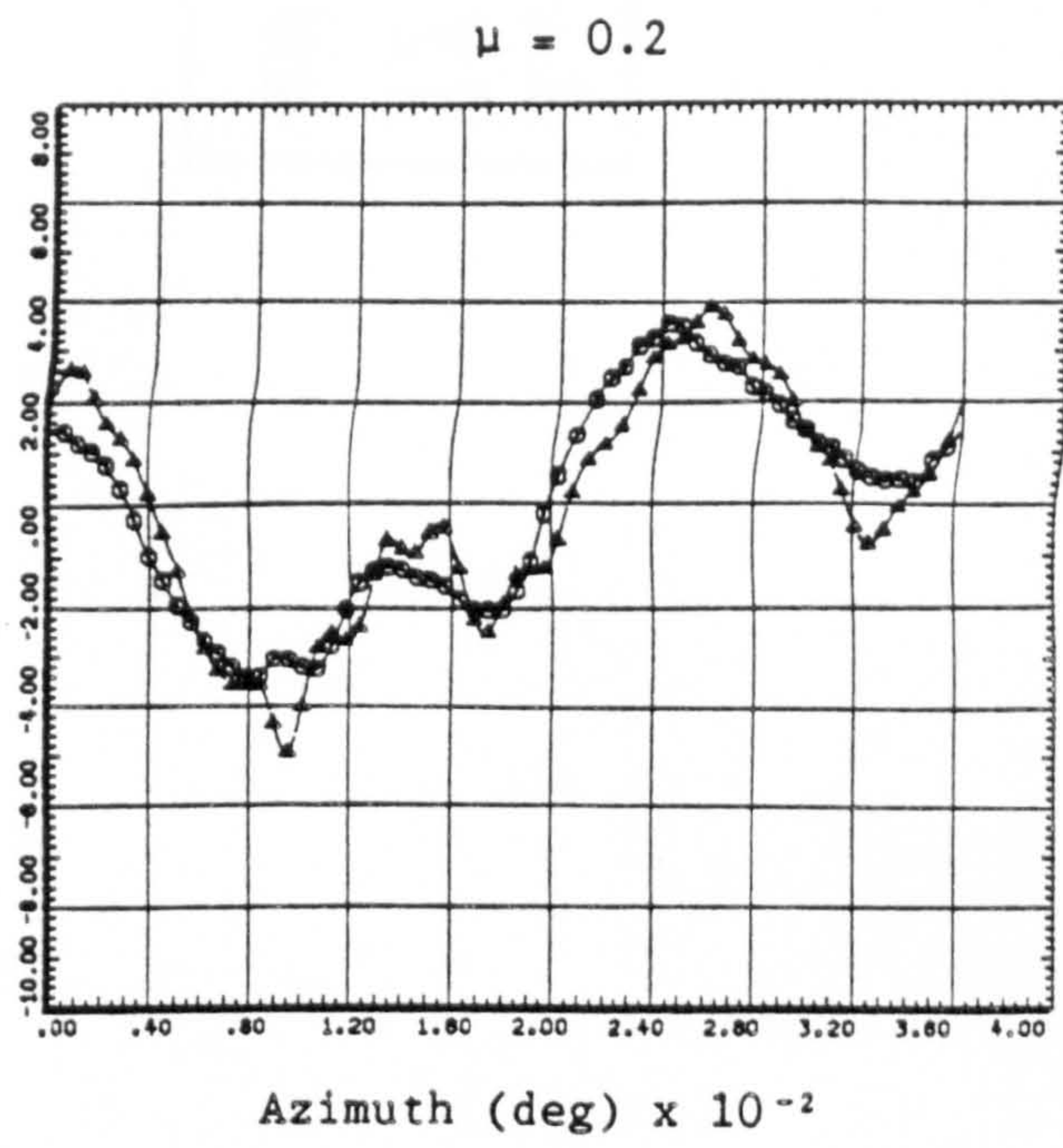
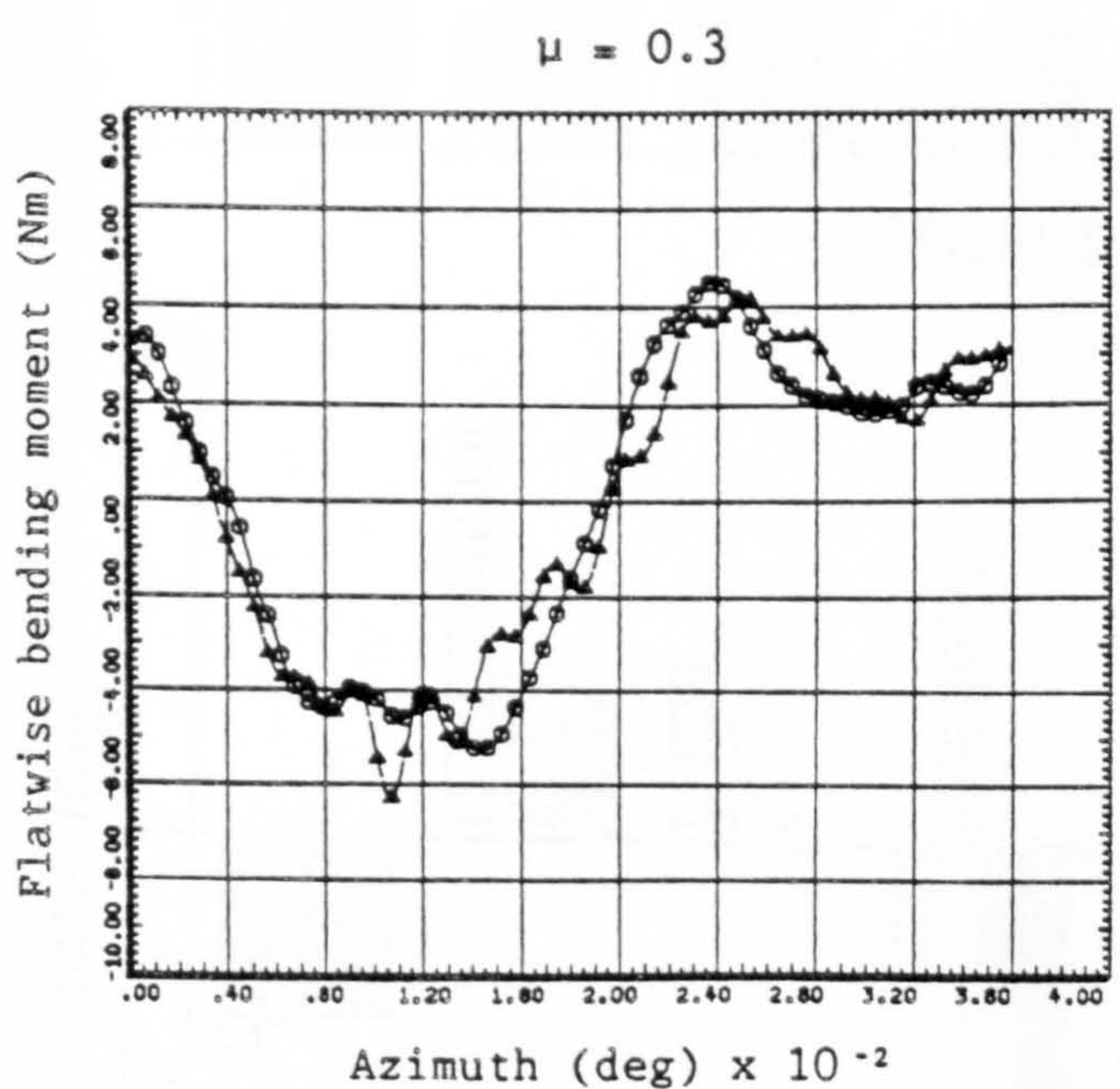
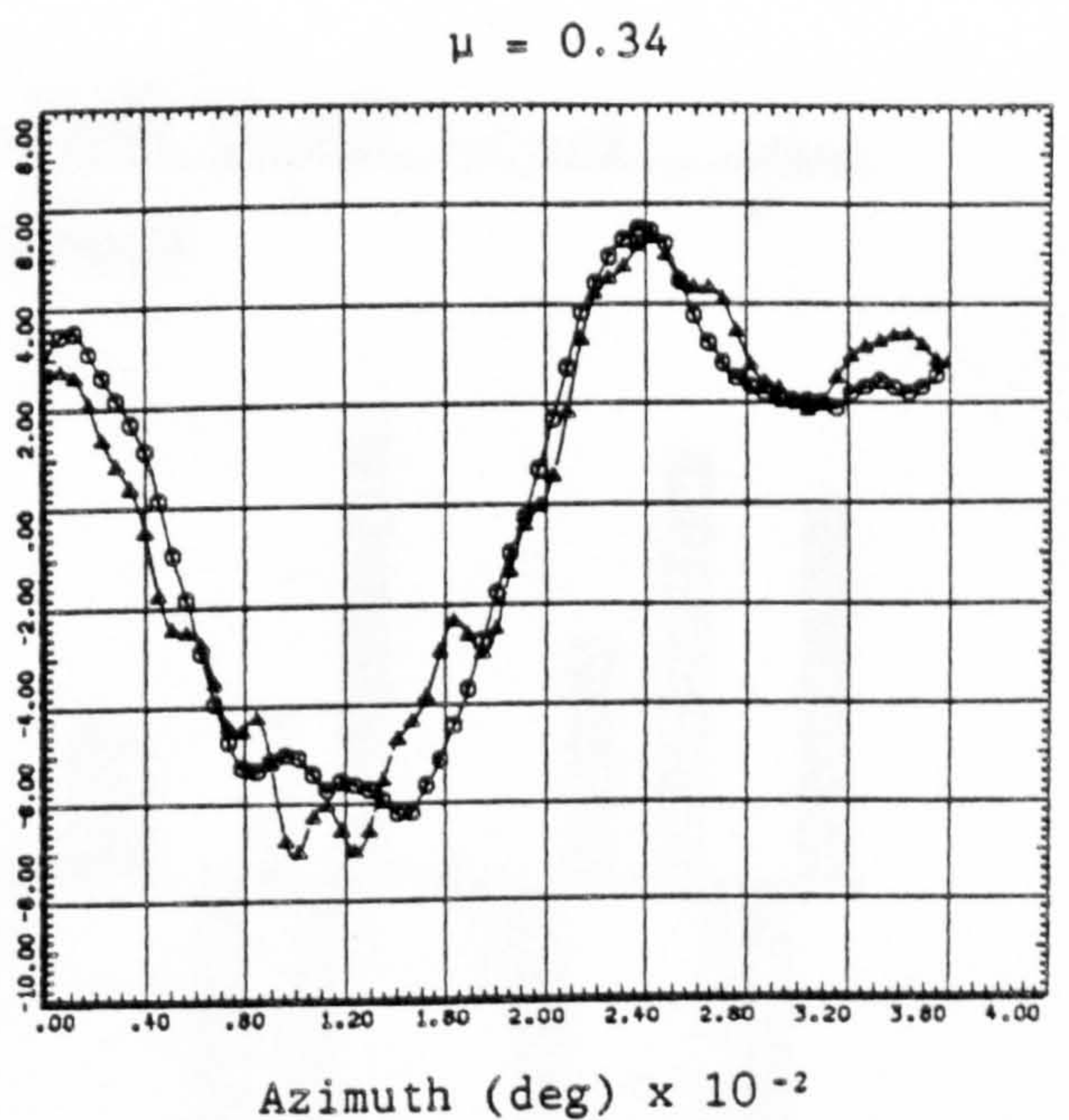


Fig 95 Root torsional moment on the Mk3 swept tip blade for various thrusts $0.1, 0.2, 0.3, 0.4, 0.5, 0.6, 0.7, 0.8, 0.9, 1.0, 1.1, 1.2, 1.3, 1.4, 1.5, 1.6, 1.7, 1.8, 1.9, 2.0, 2.1, 2.2, 2.3, 2.4, 2.5, 2.6, 2.7, 2.8, 2.9, 3.0, 3.1, 3.2, 3.3, 3.4, 3.5, 3.6, 3.7, 3.8, 3.9, 4.0, 4.1, 4.2, 4.3, 4.4, 4.5, 4.6, 4.7, 4.8, 4.9, 5.0$



Experiment

Theory

Fig 96 Measured and predicted (J146) flatwise bending moments on the Mk3 swept tip blade, thrust = 900N, 37% R

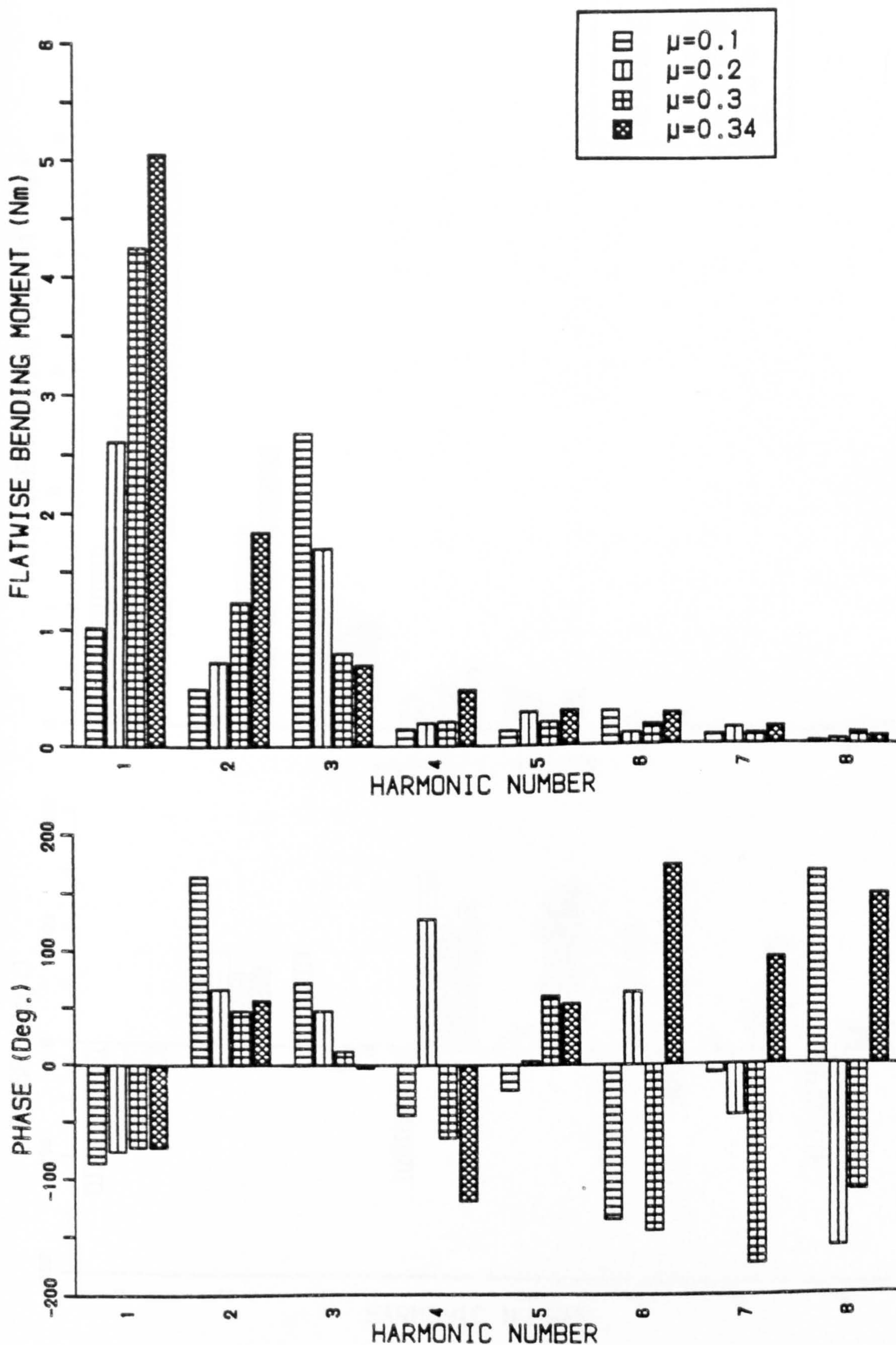


Fig 97 Harmonic analysis of the predicted (J146) flatwise bending moments on the Mk3 swept tip blade, thrust = 900N, 37% R

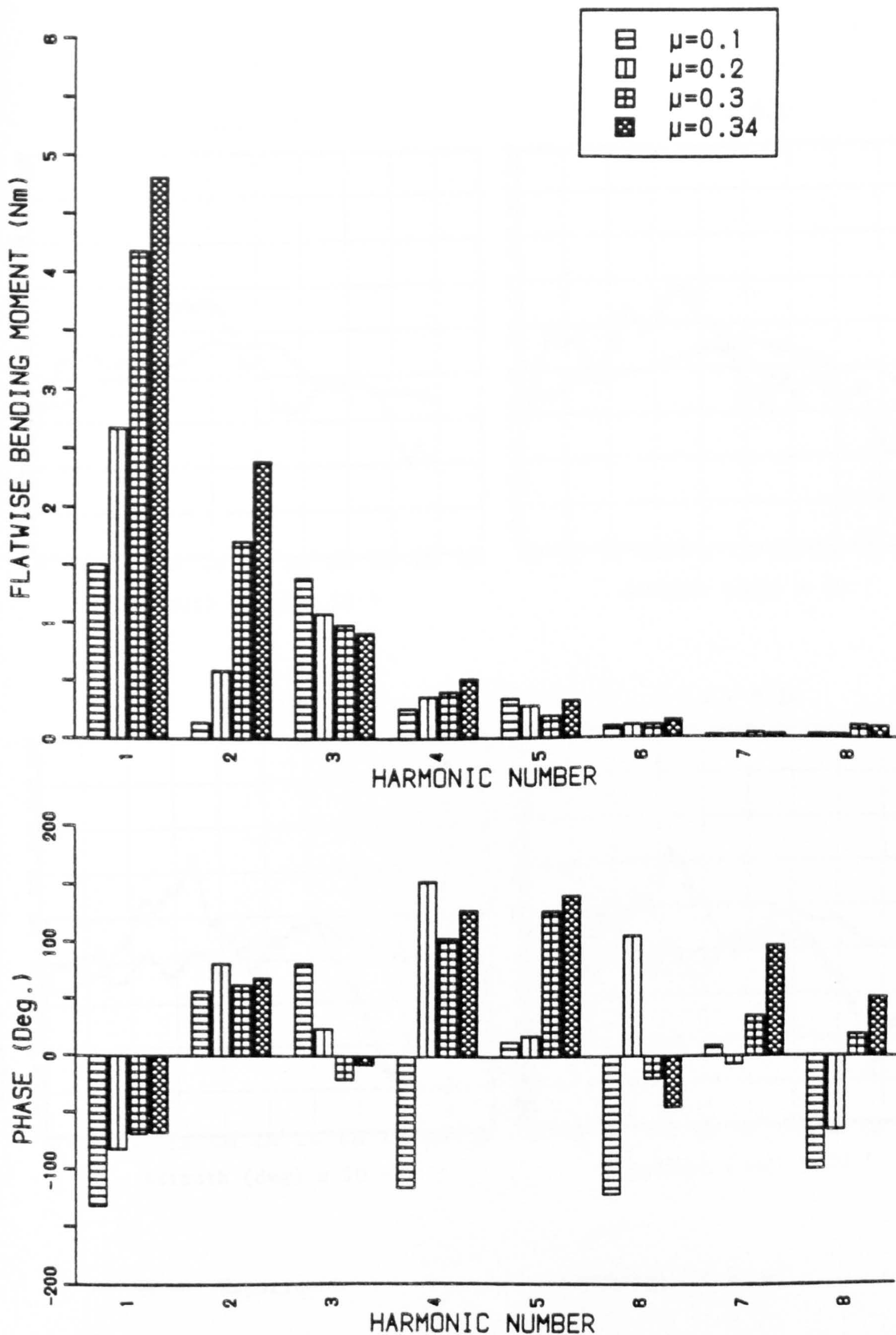
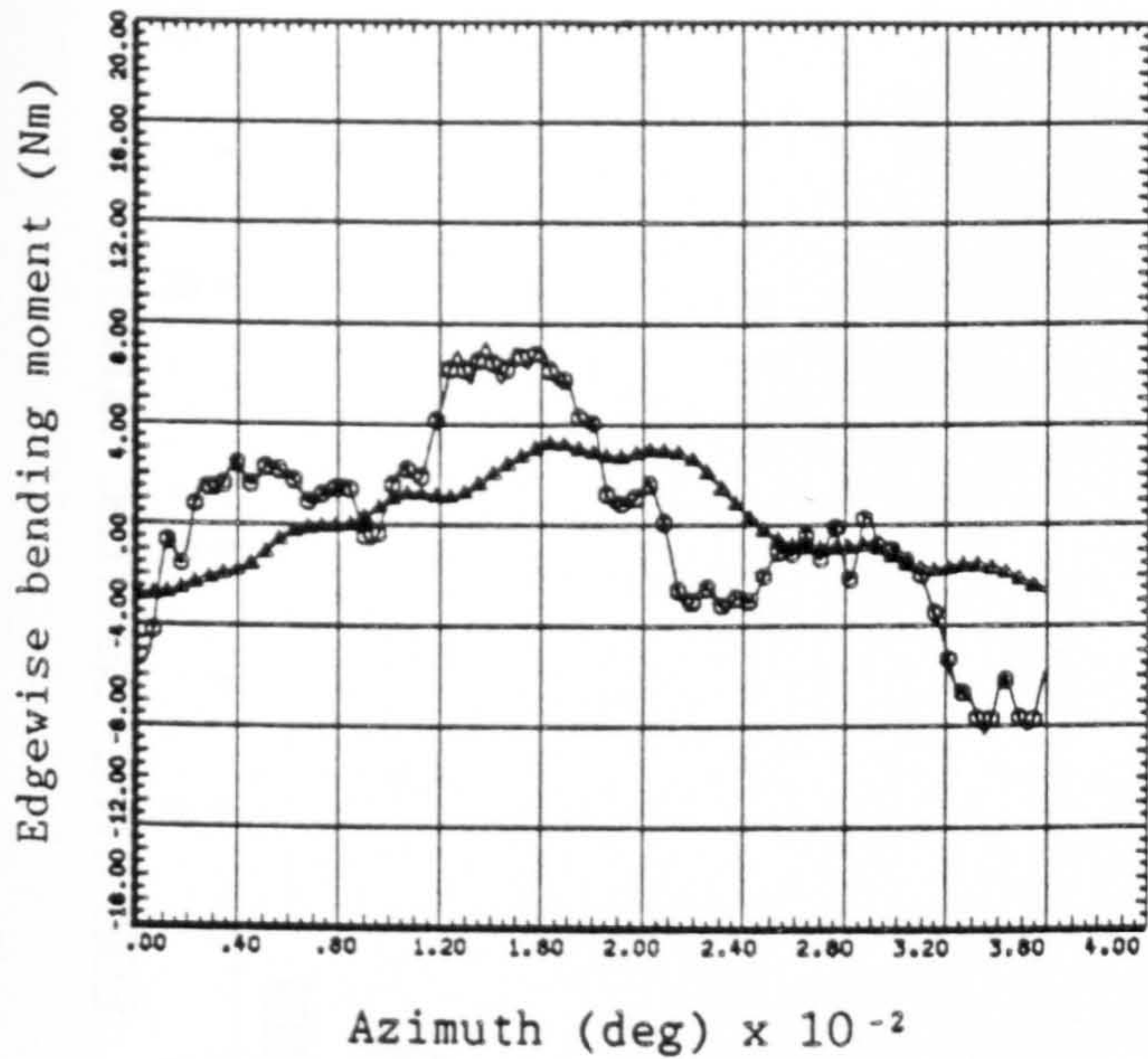
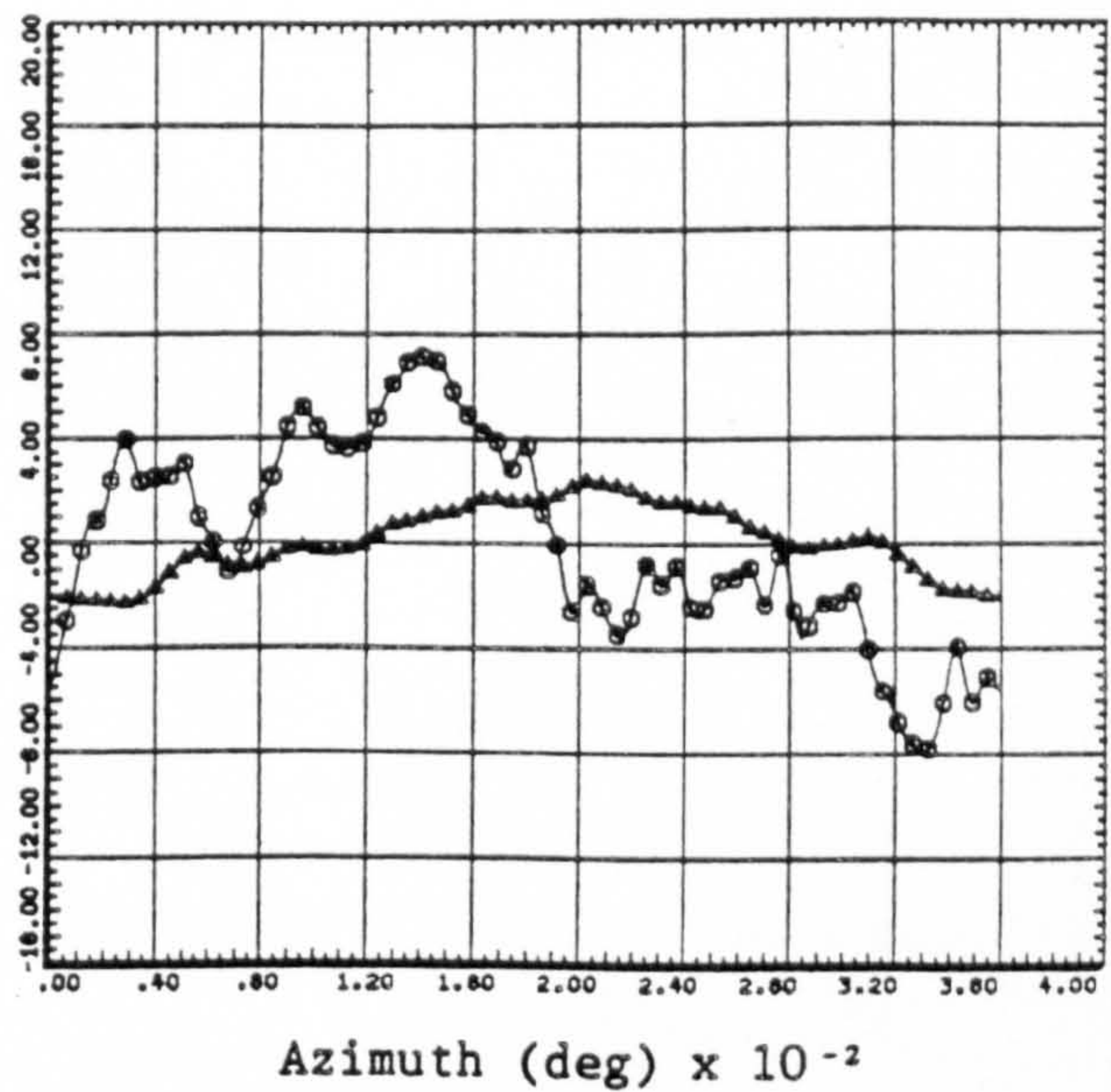


Fig 98 Harmonic analysis of the measured flatwise bending moments on the Mk3 swept tip blade, thrust = 900N, 37% R

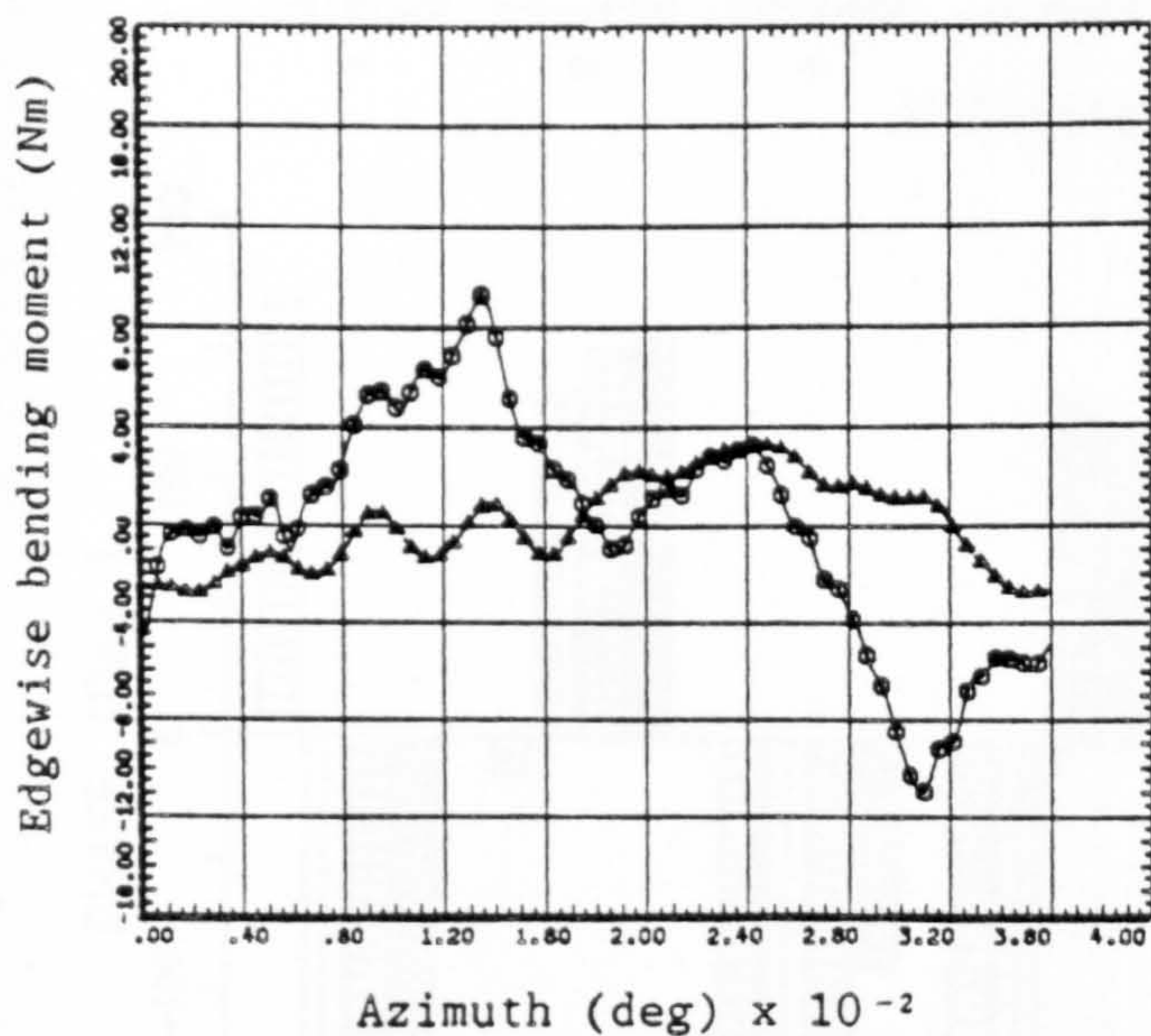
$\mu = 0.1$



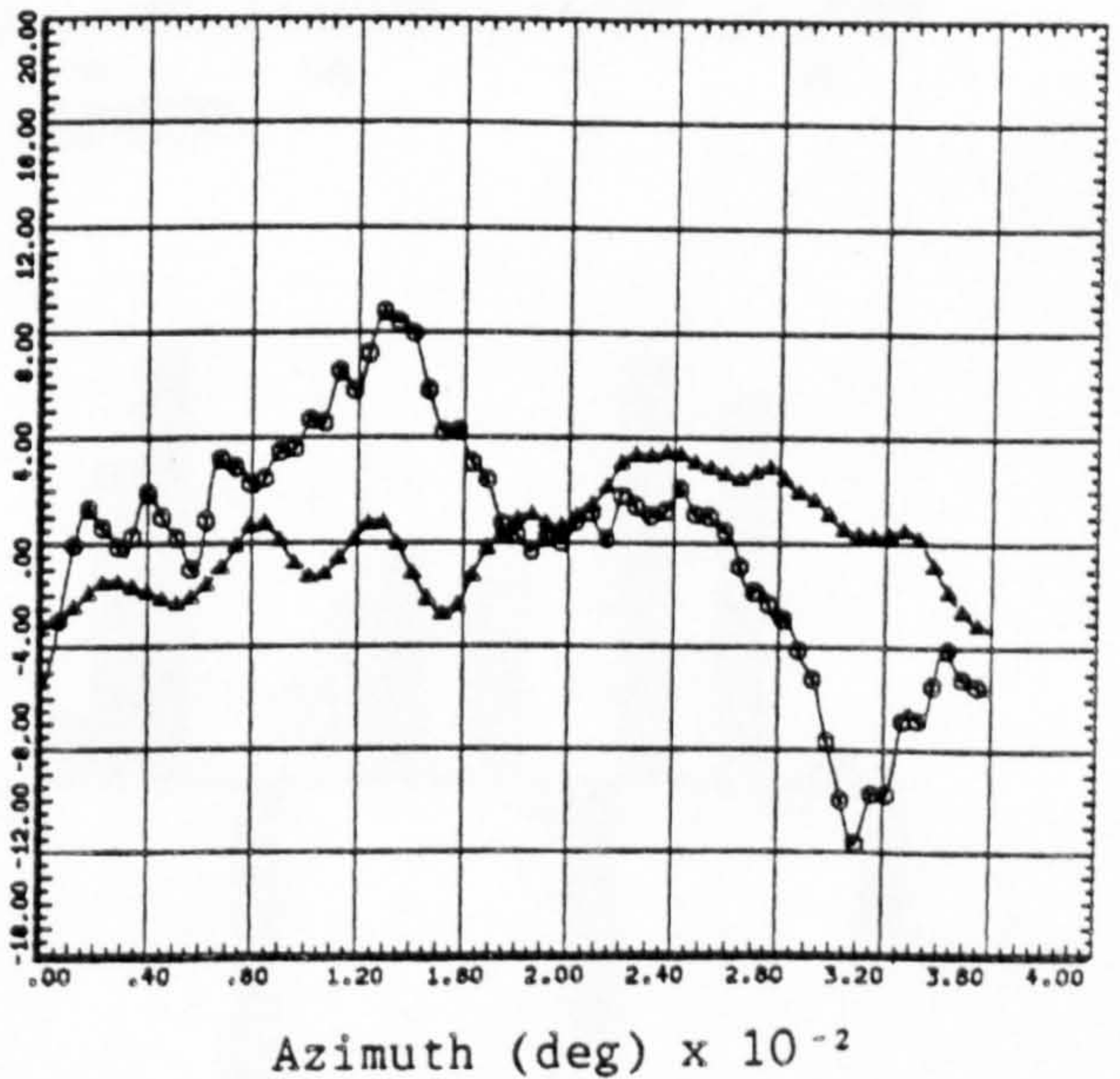
$\mu = 0.2$



$\mu = 0.3$



$\mu = 0.34$



—○— Experiment

—△— Theory

Fig 99 Measured and predicted (J146) edgewise bending moments on the Mk3 swept tip blade, thrust = 900N, 37% R

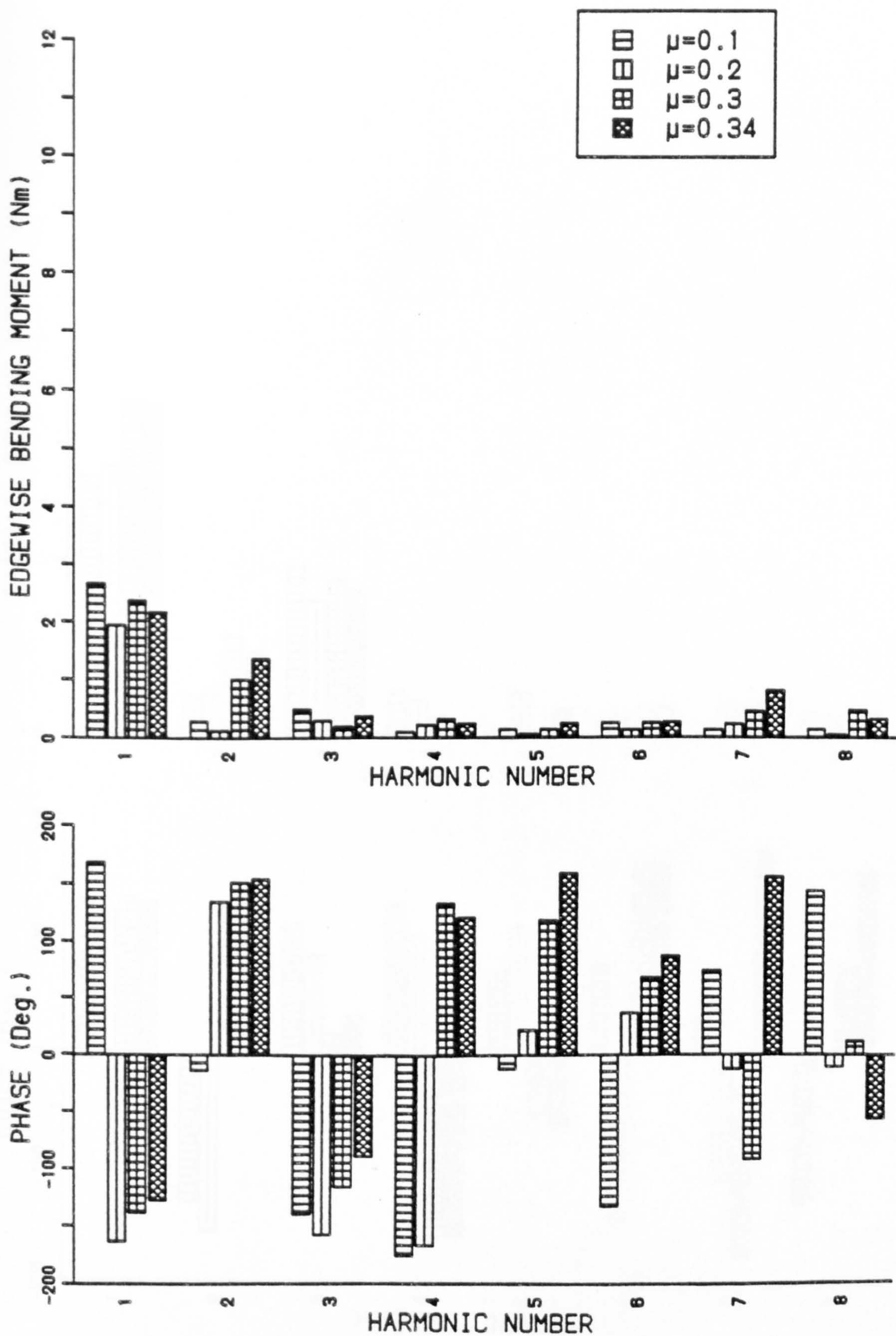


Fig 100 Harmonic analysis of the predicted (J146) edgewise bending moments on the Mk3 swept tip blade, thrust = 900N, 37% R

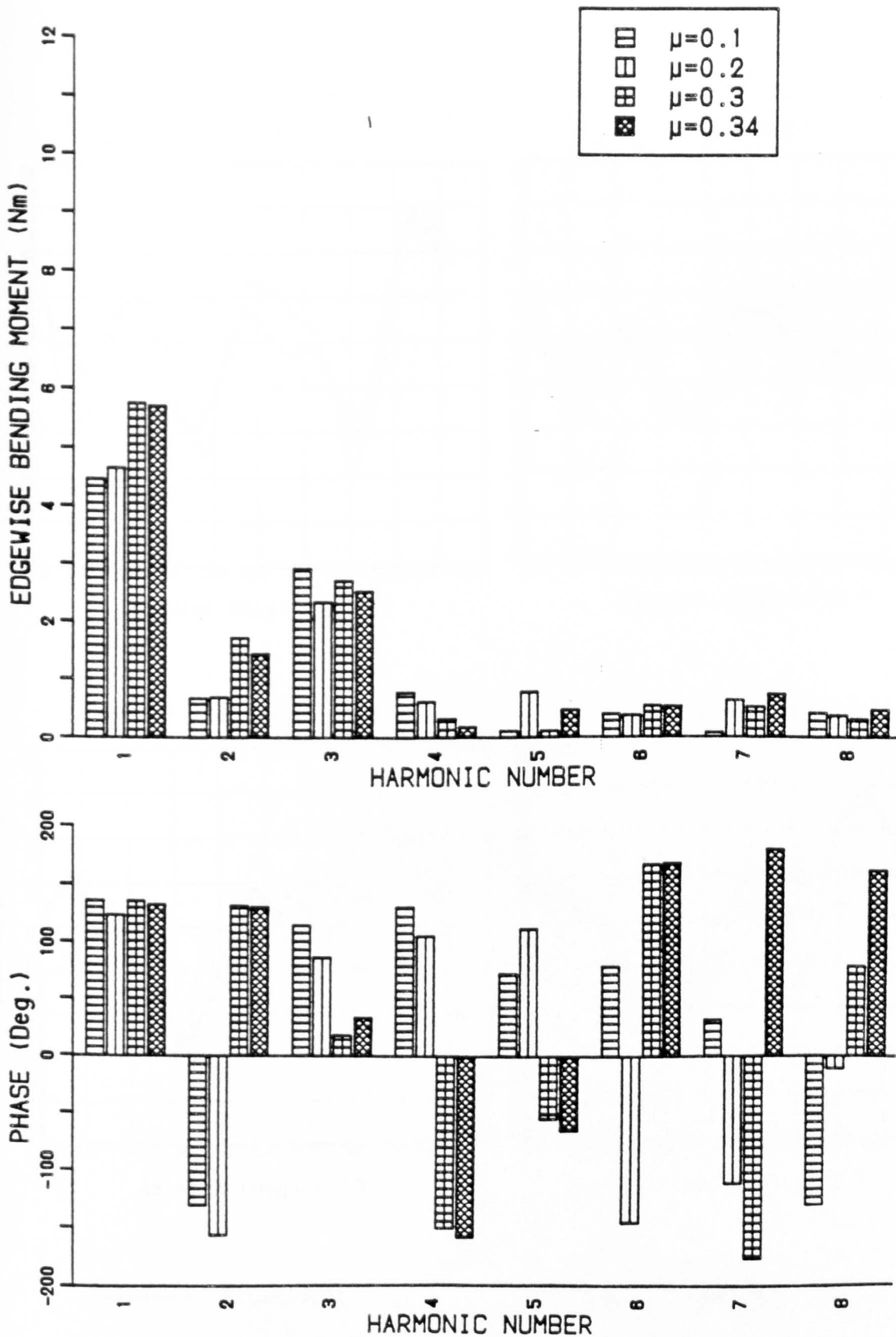
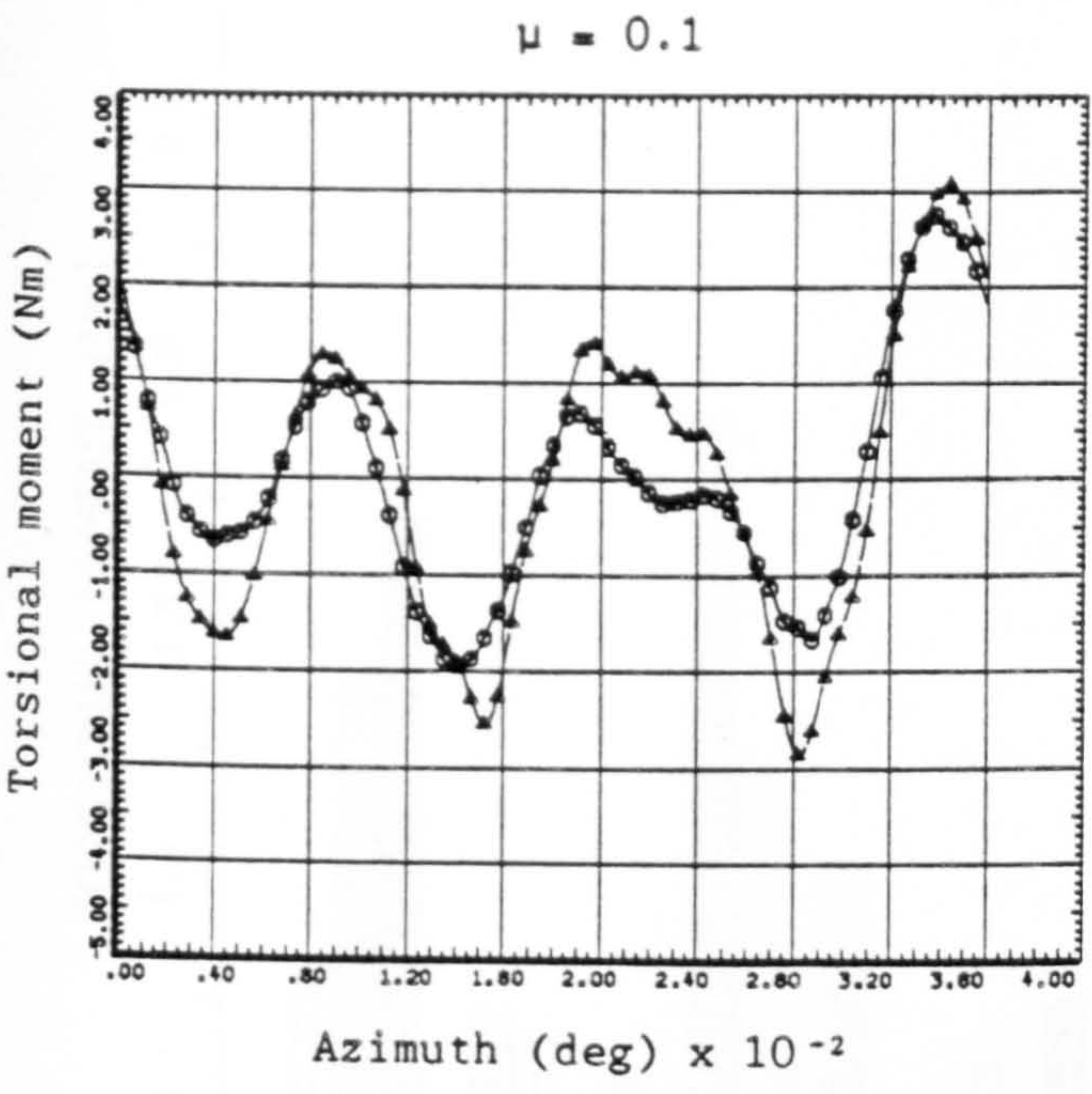
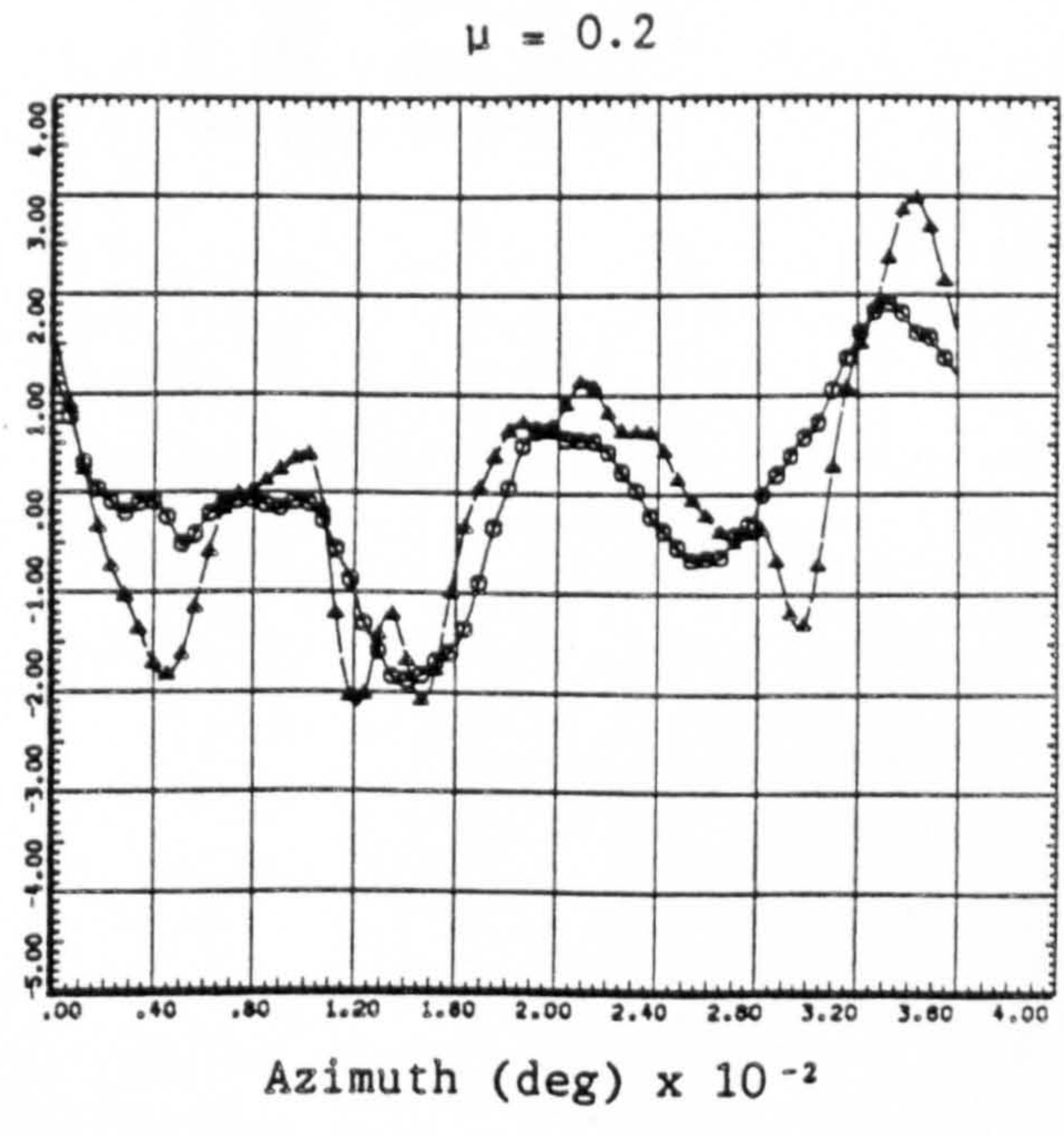
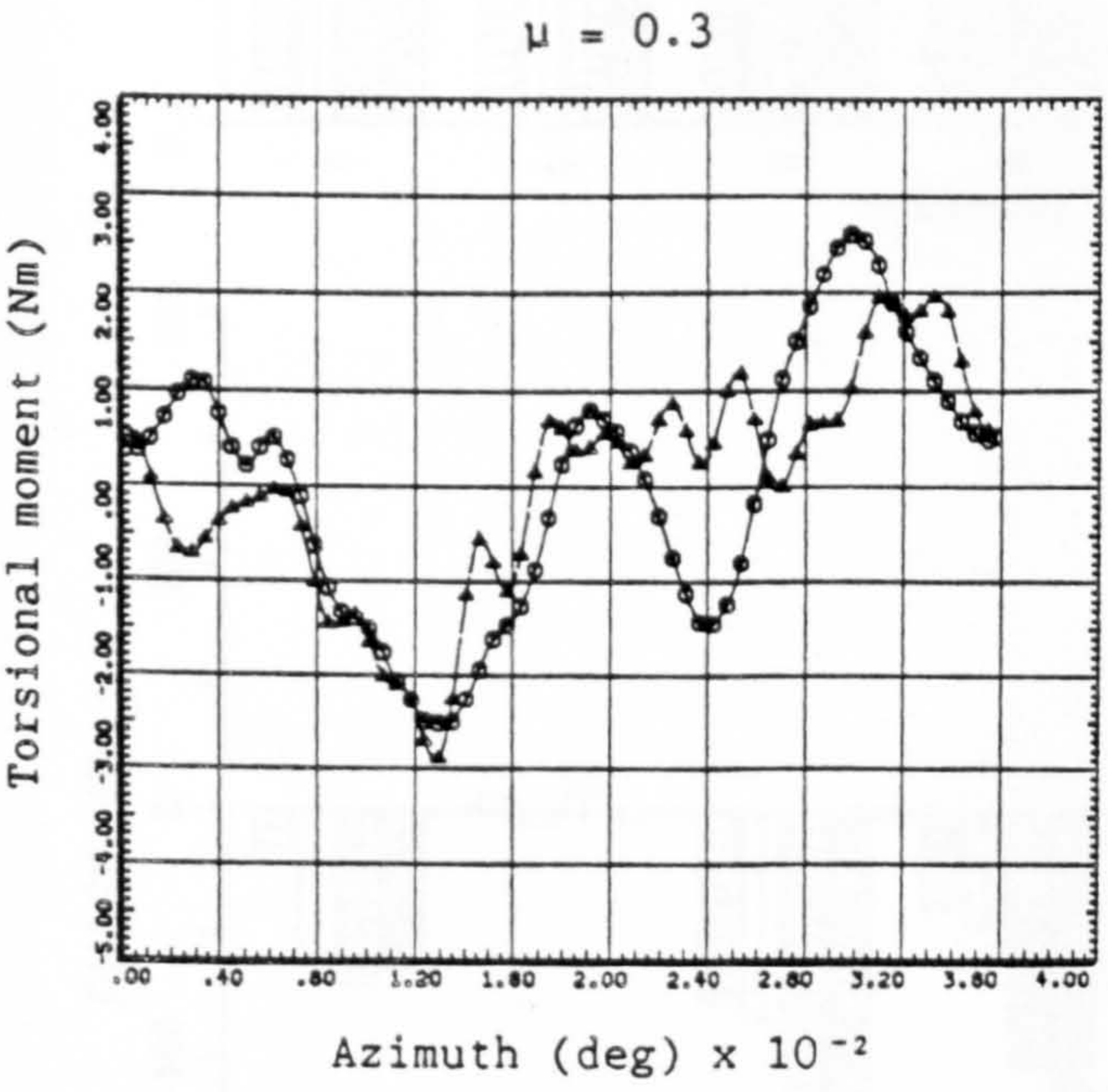
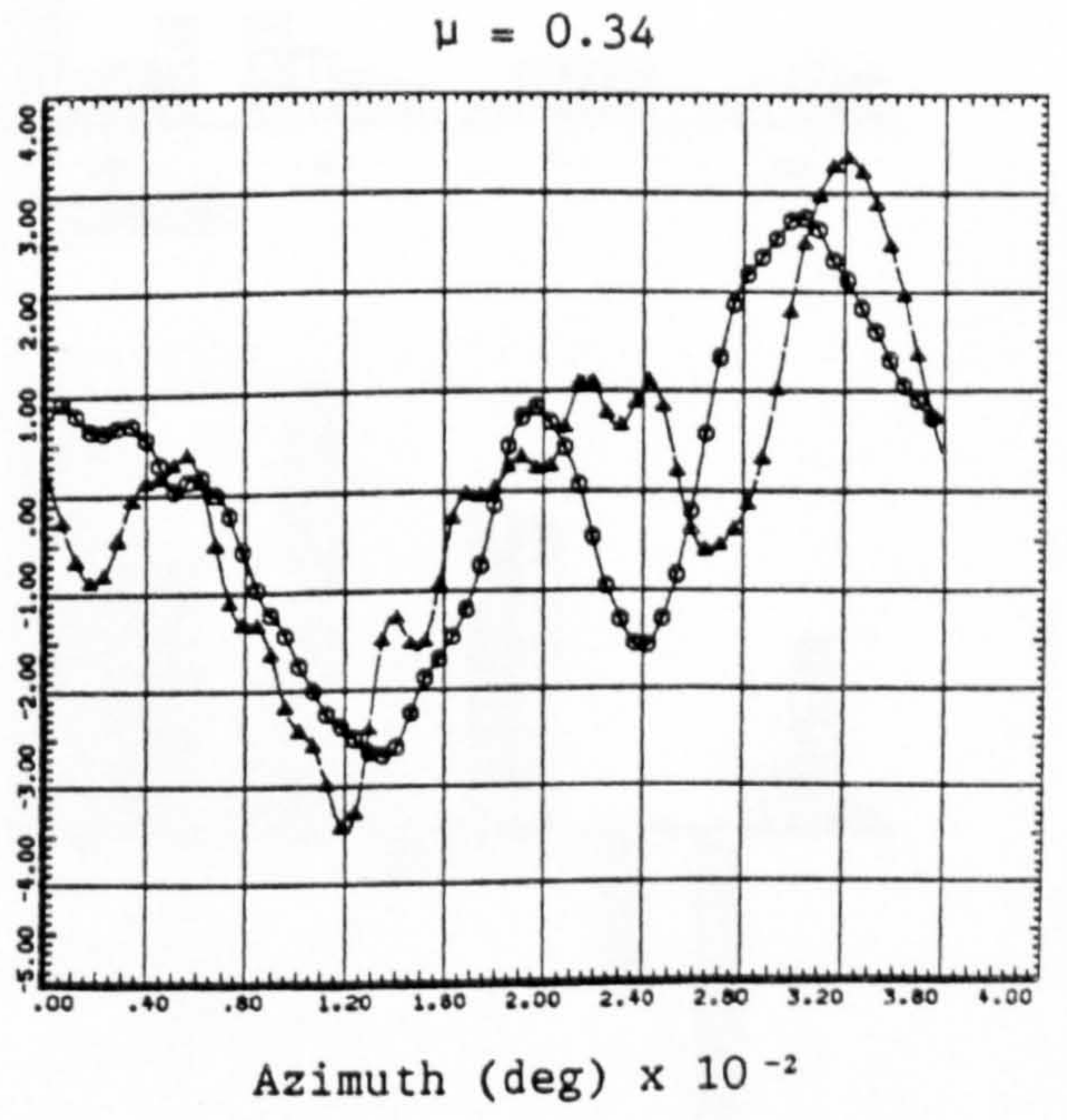


Fig 101 Harmonic analysis of the measured edgewise bending moments on the Mk3 swept tip blade, thrust = 900N, 37% R



—○— Experiment

—△— Theory

Fig 102 Measured and predicted (J146) torsional moments on the Mk3 swept tip blade, thrust = 900N, 37% R

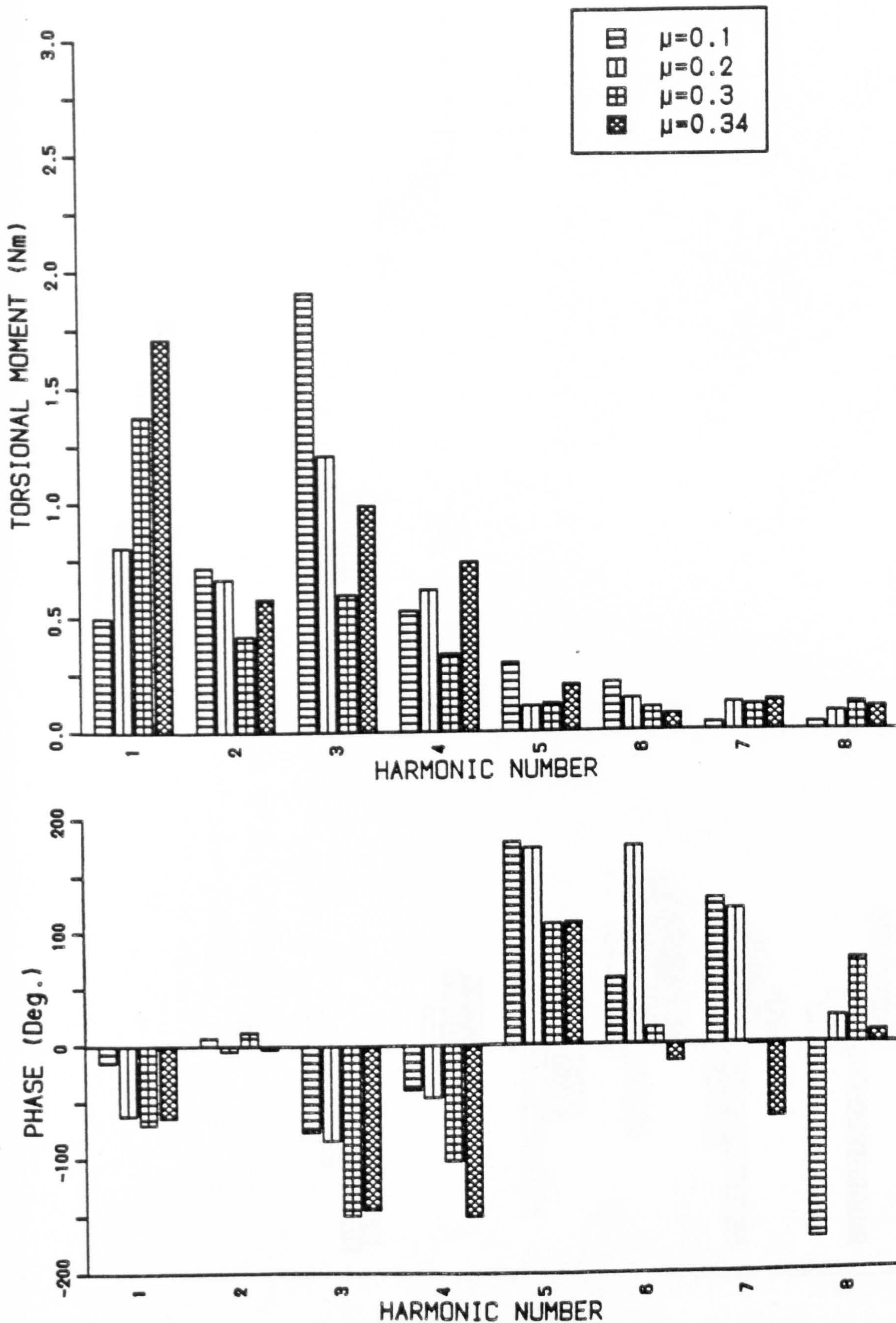


Fig 103 Harmonic analysis of the predicted (J146) torsional moments on the Mk3 swept tip blade, thrust = 900N, 37% R

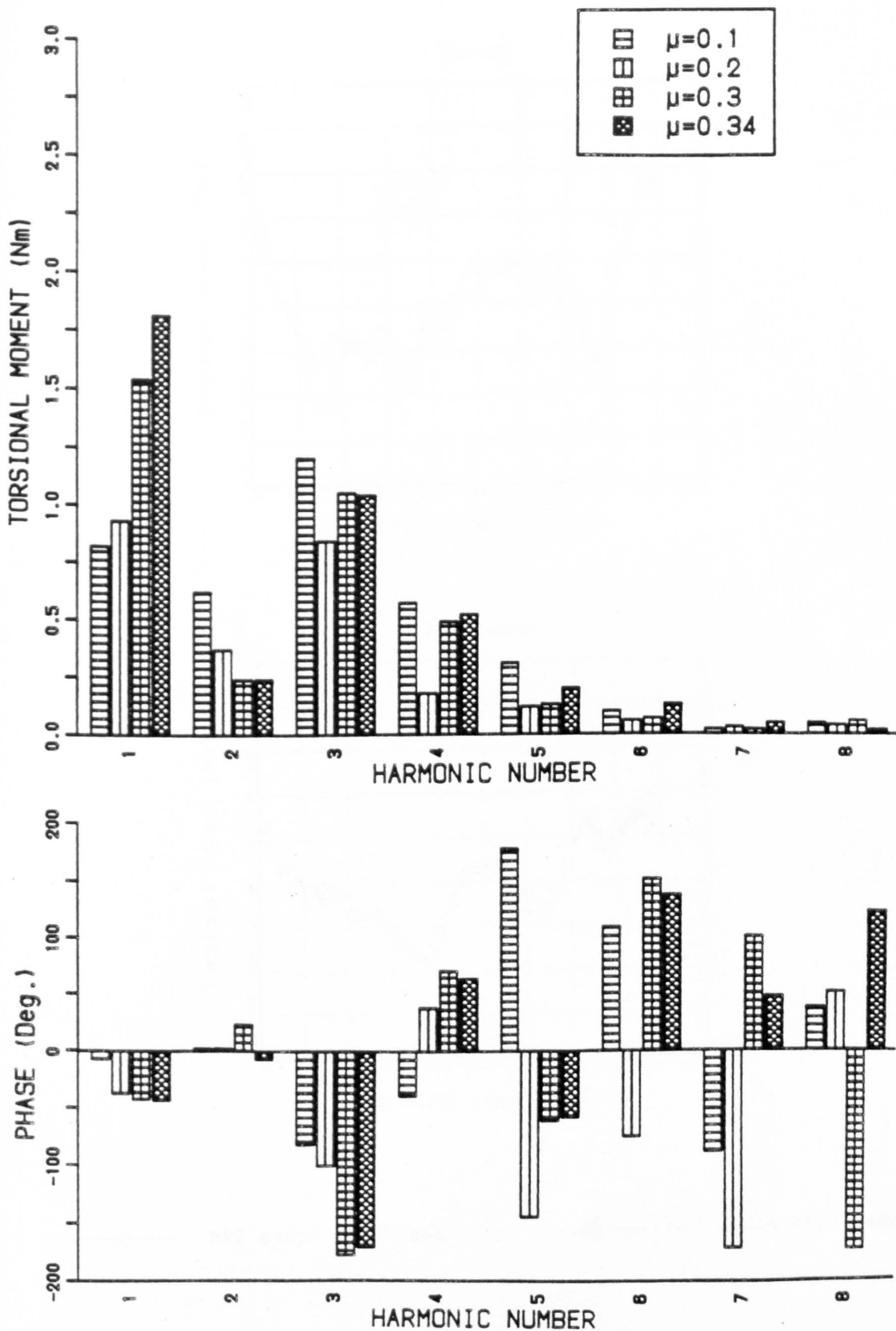
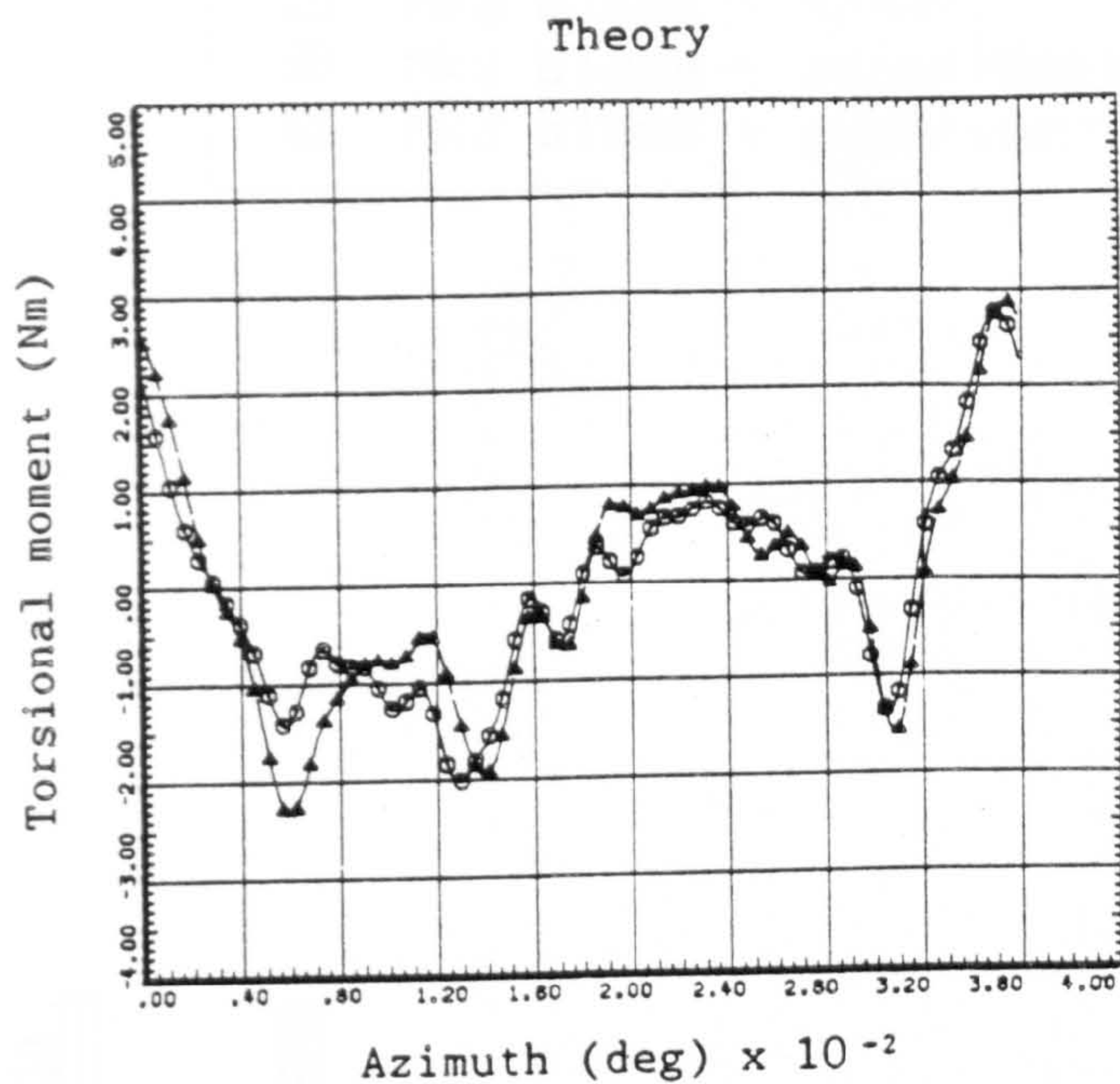
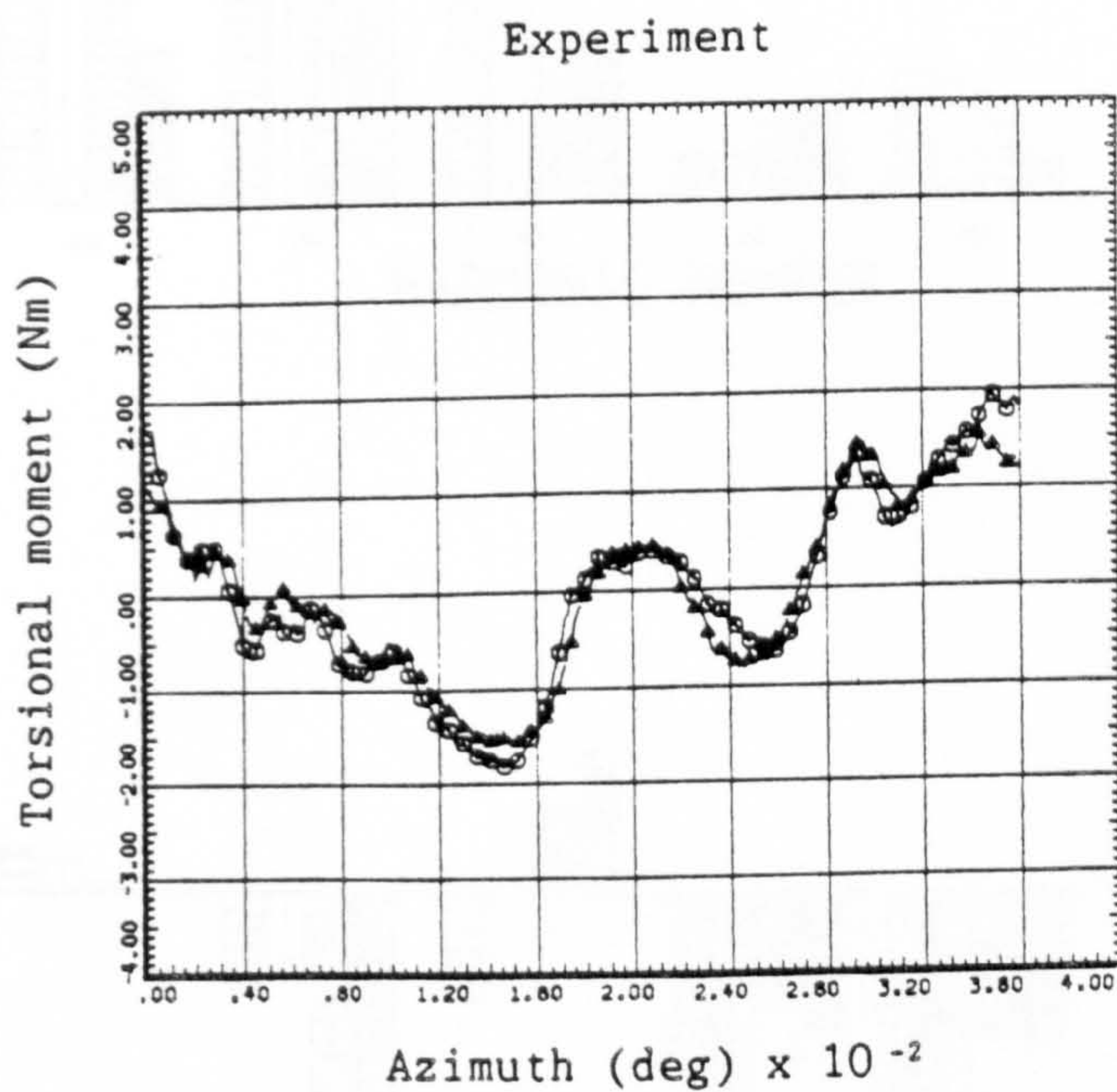


Fig 104 Harmonic analysis of the measured torsional moments on the Mk3 swept tip blade, thrust = 900N, 37% R



Mk2 swept tip blade



Mk3 swept tip blade

Fig 105 Measured and predicted (J146) torsional moments on the Mk2 and Mk3 swept tip blades, thrust = 900N, $\mu = 0.24$, 30% R

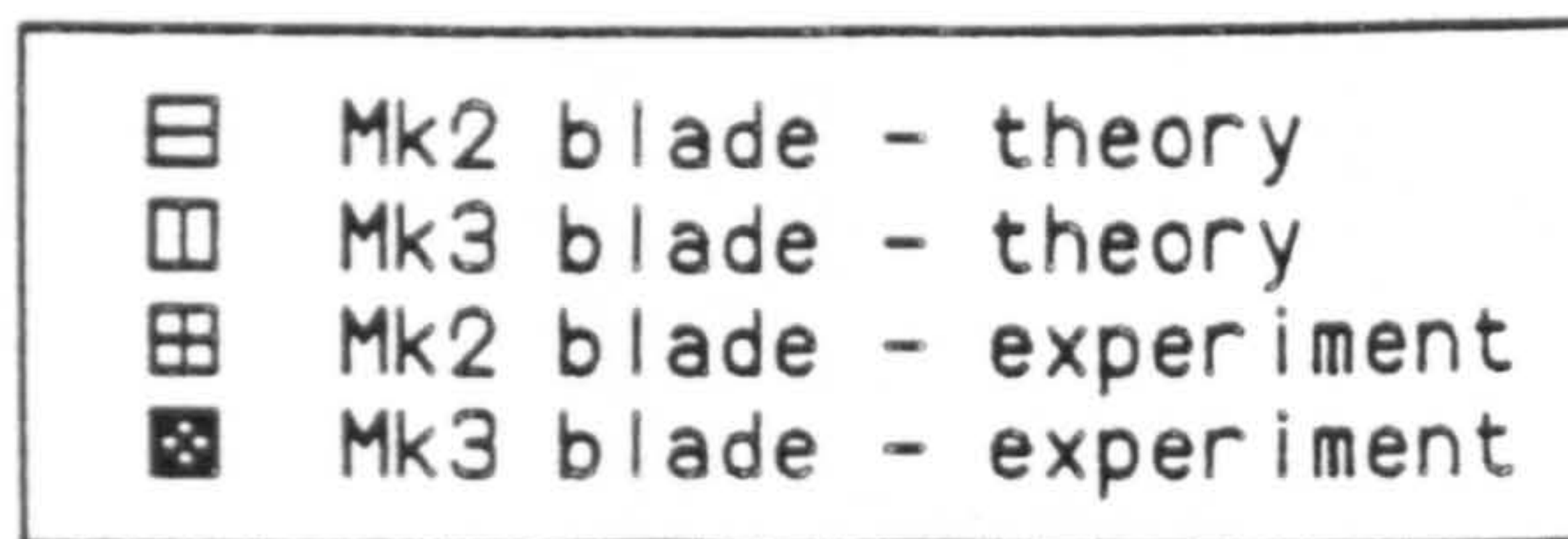
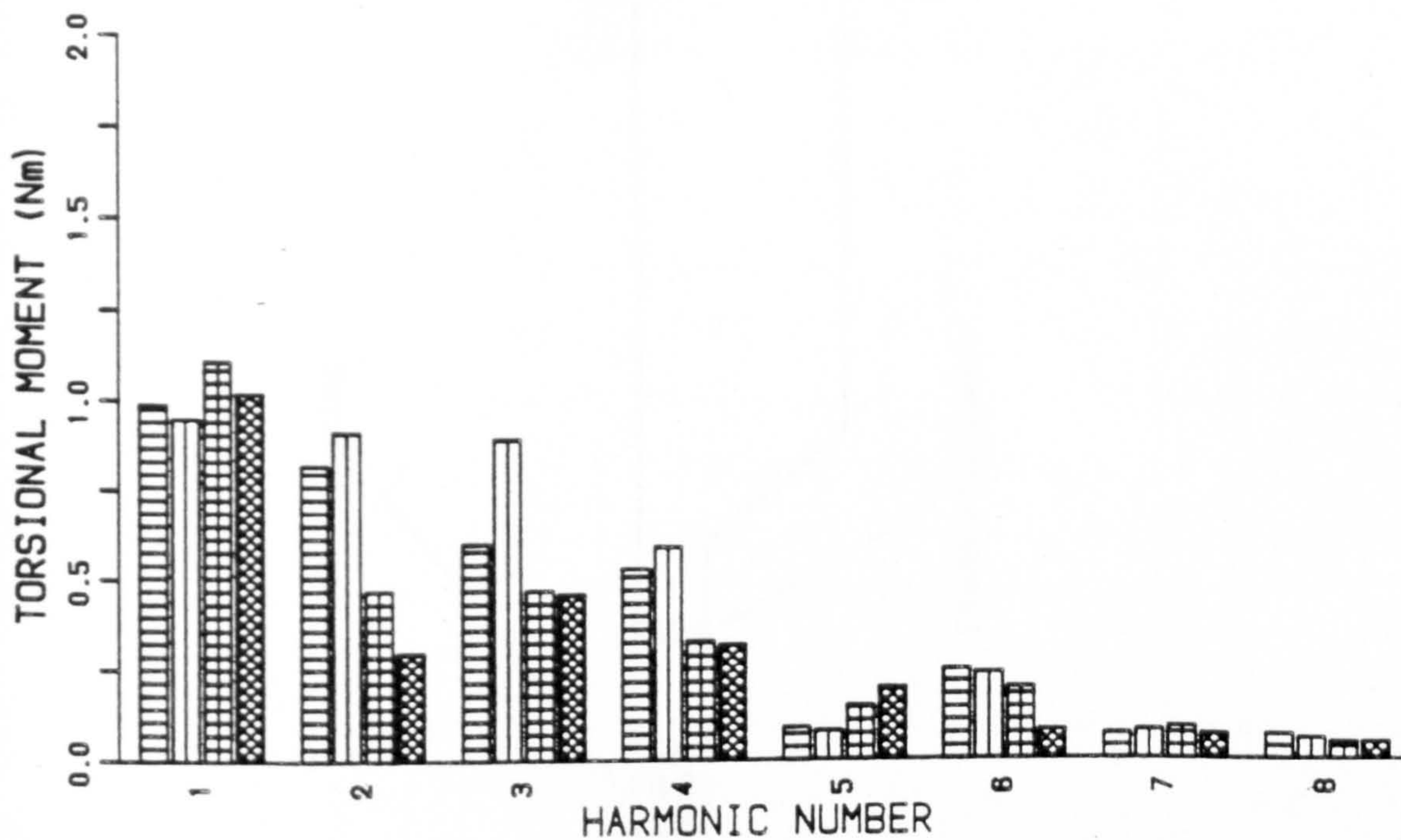
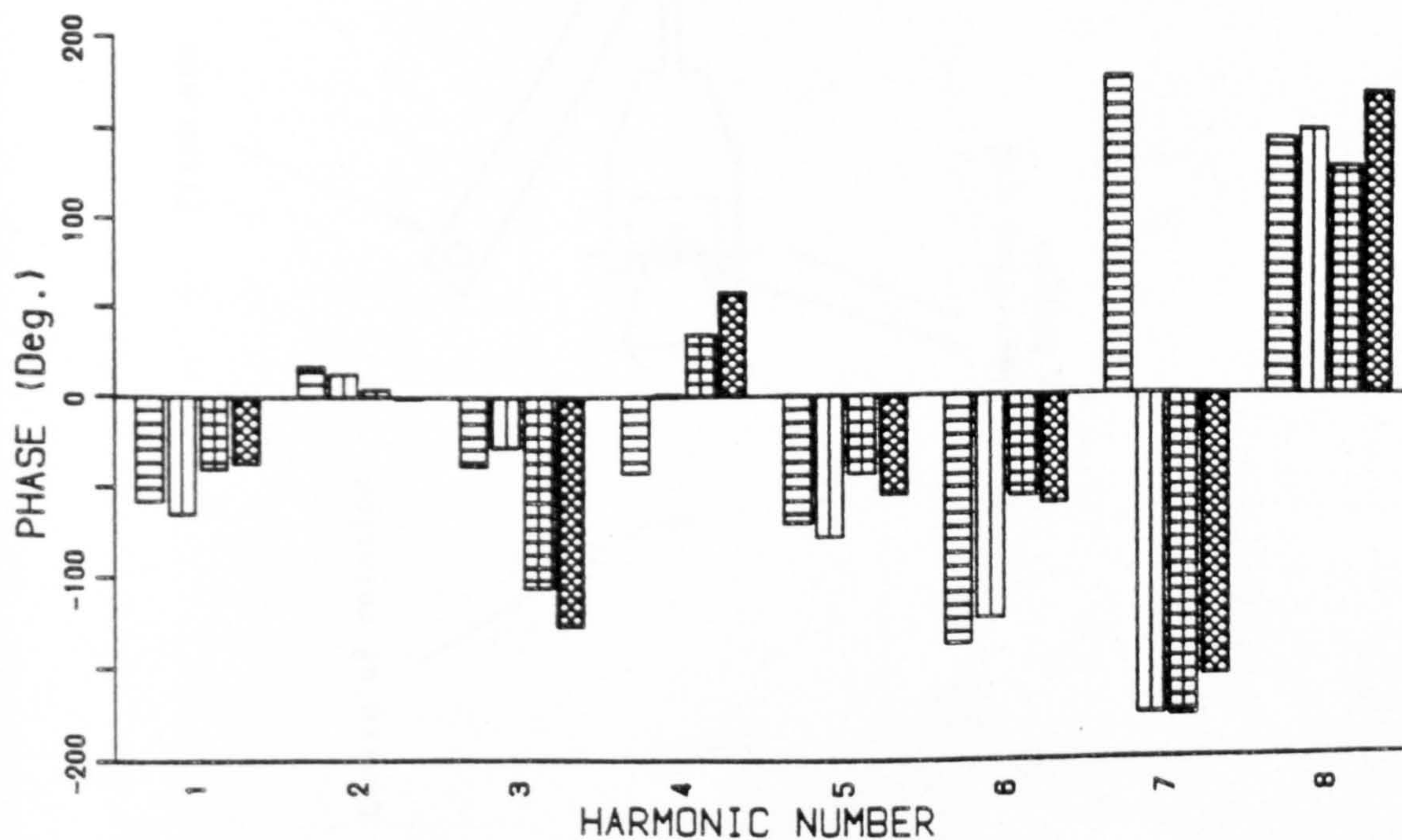


Fig 106 Harmonic analysis of the measured and predicted (J146) torsional moments on the Mk2 and Mk3 swept tip blades, thrust = 900N, $\mu = 0.24$, 30% R

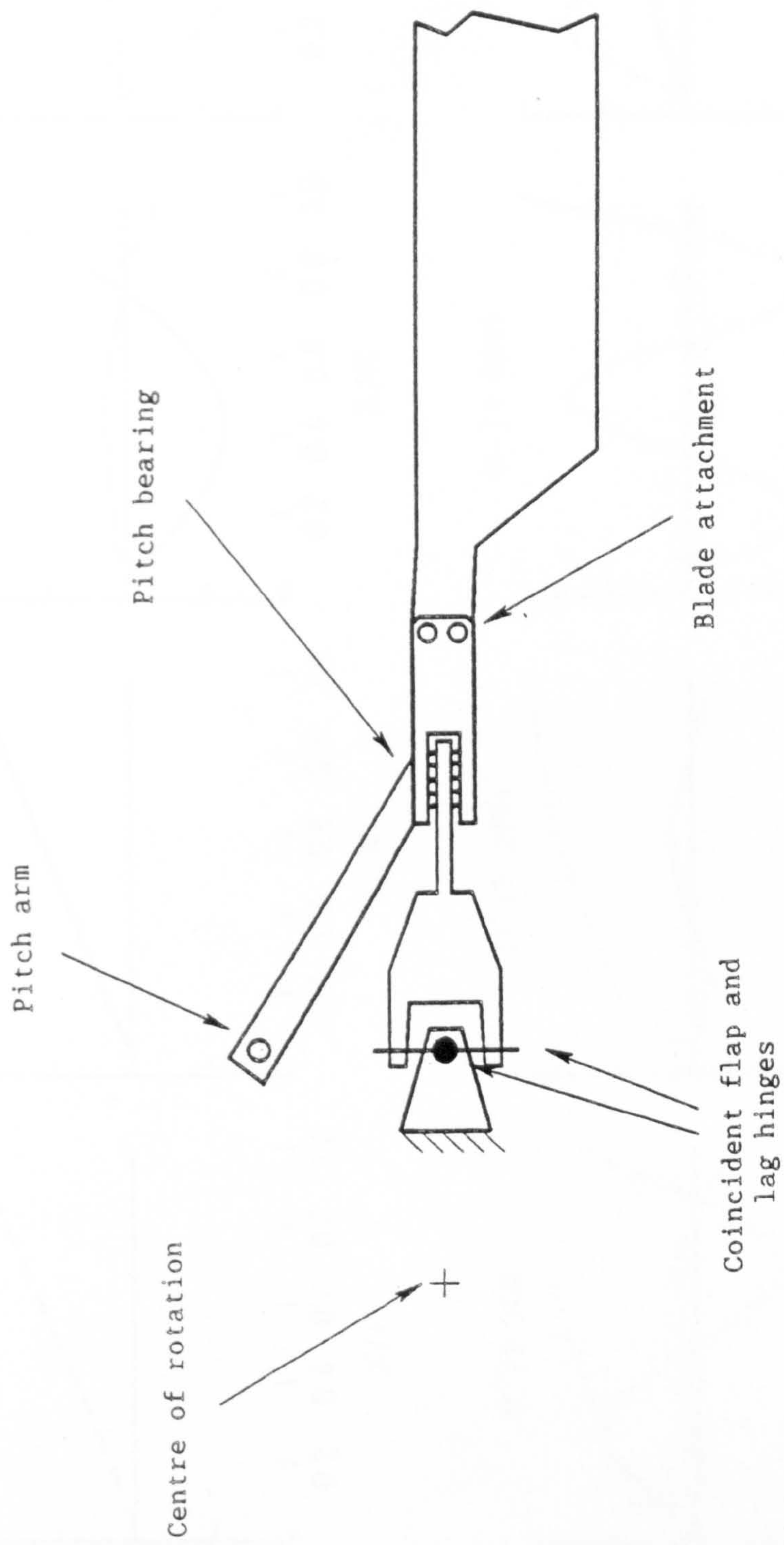


Fig 107 Representation of the Puma rotor in the J134 modes program

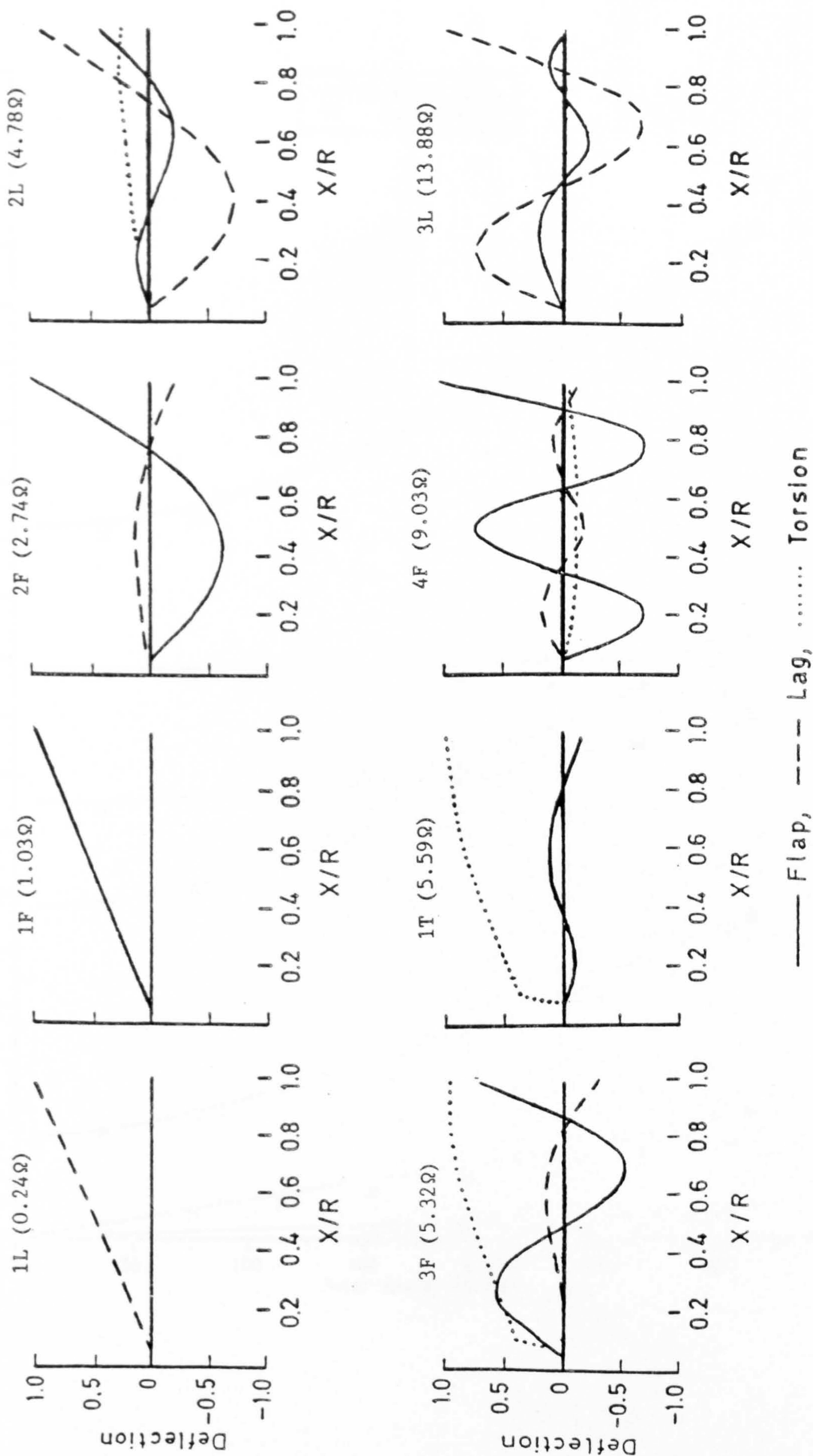


Fig 108 J134 mode shapes for the baseline Puma blade (267 rev/min)

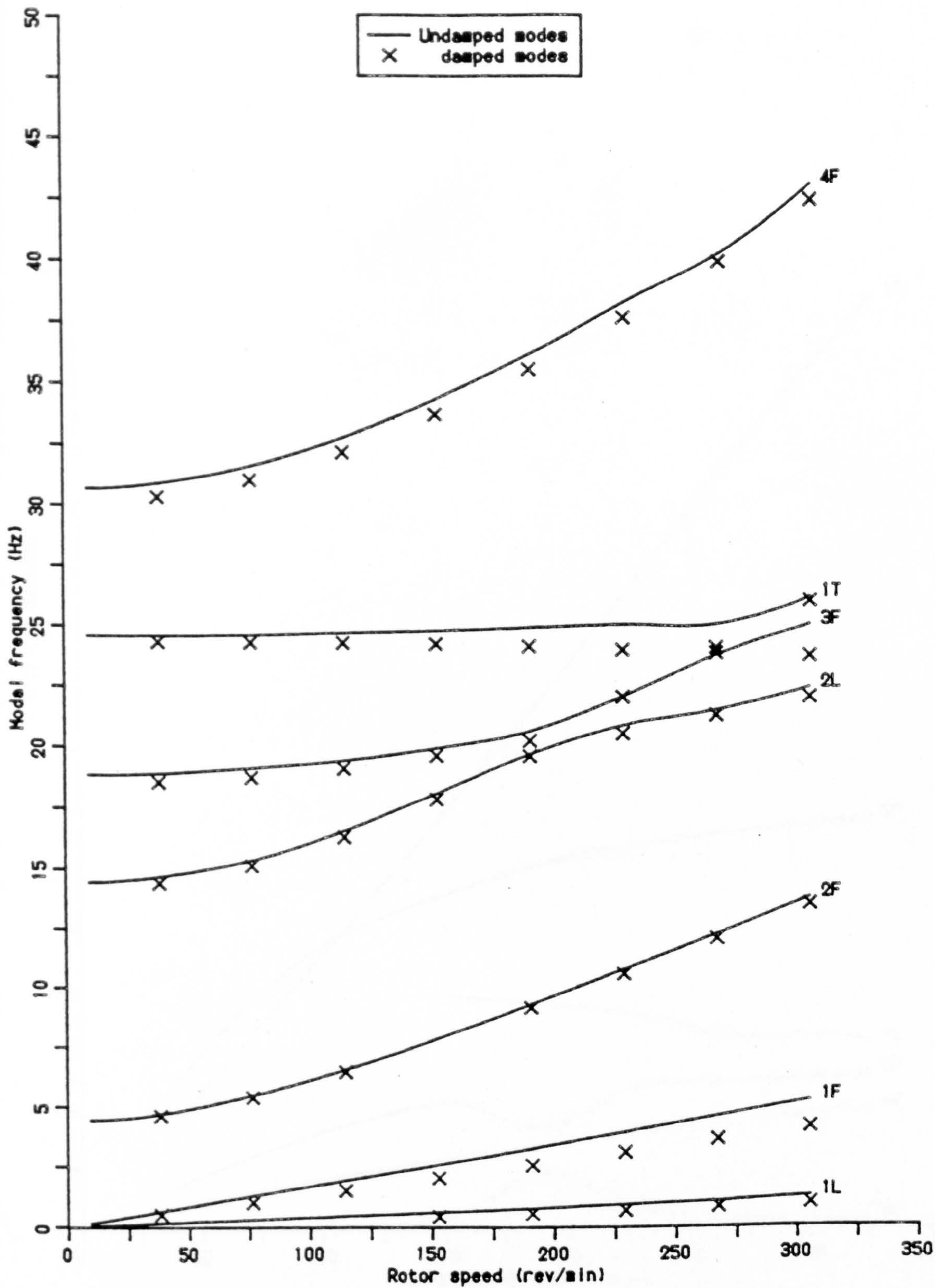


Fig 109 Damped and undamped modal frequencies for the baseline Puma blade

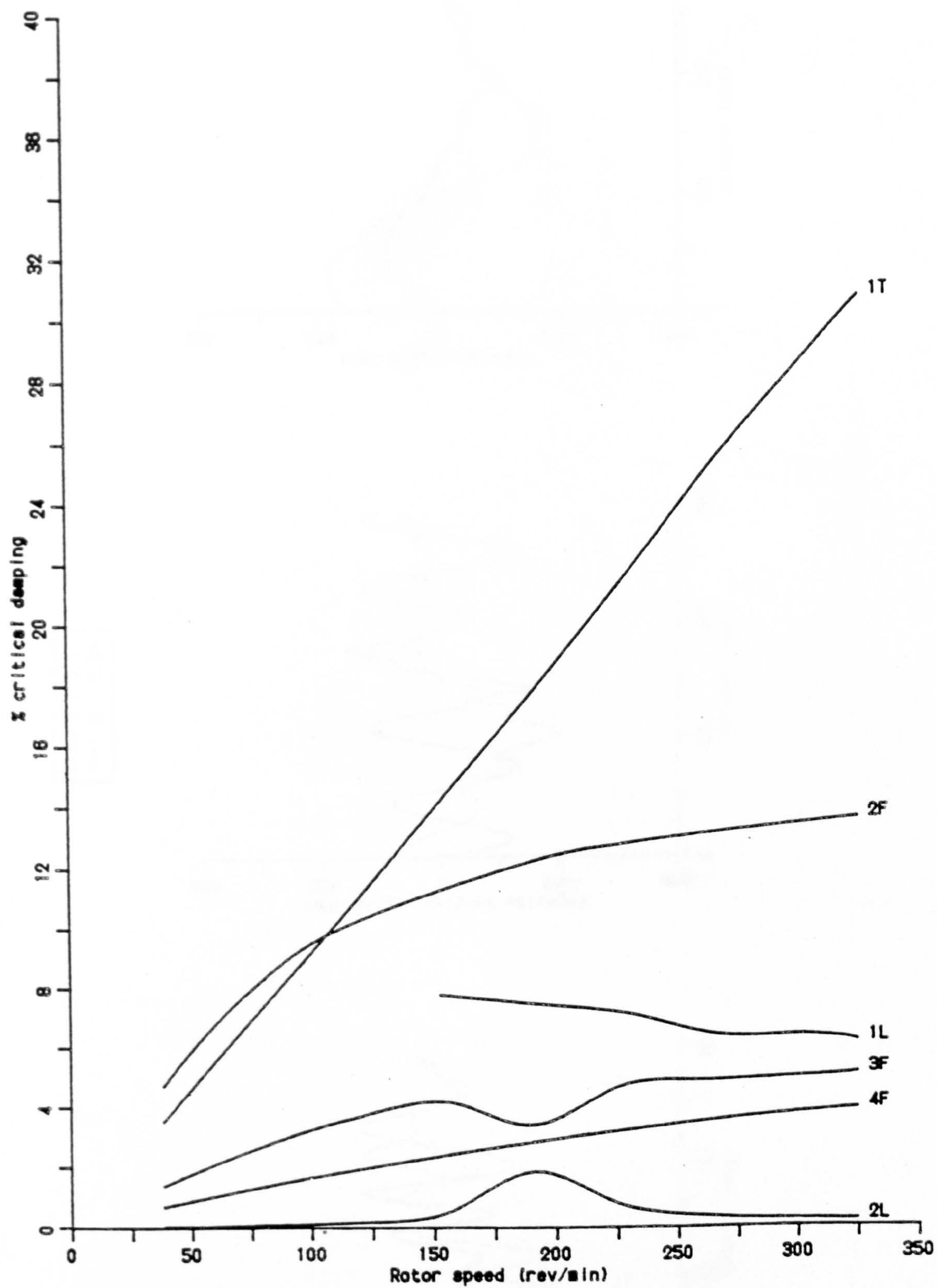


Fig 110 Modal dampings for the baseline Puma blade

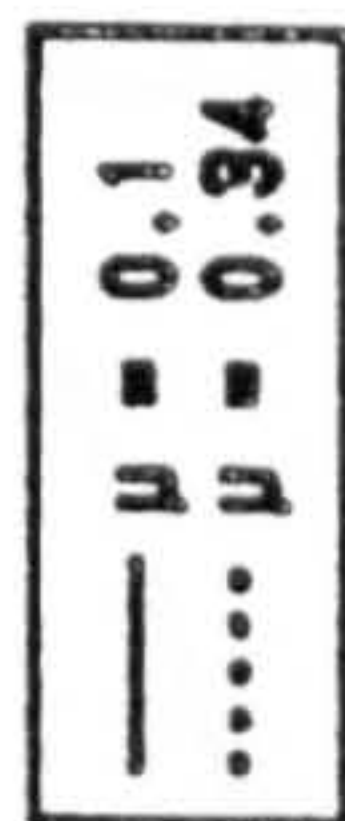
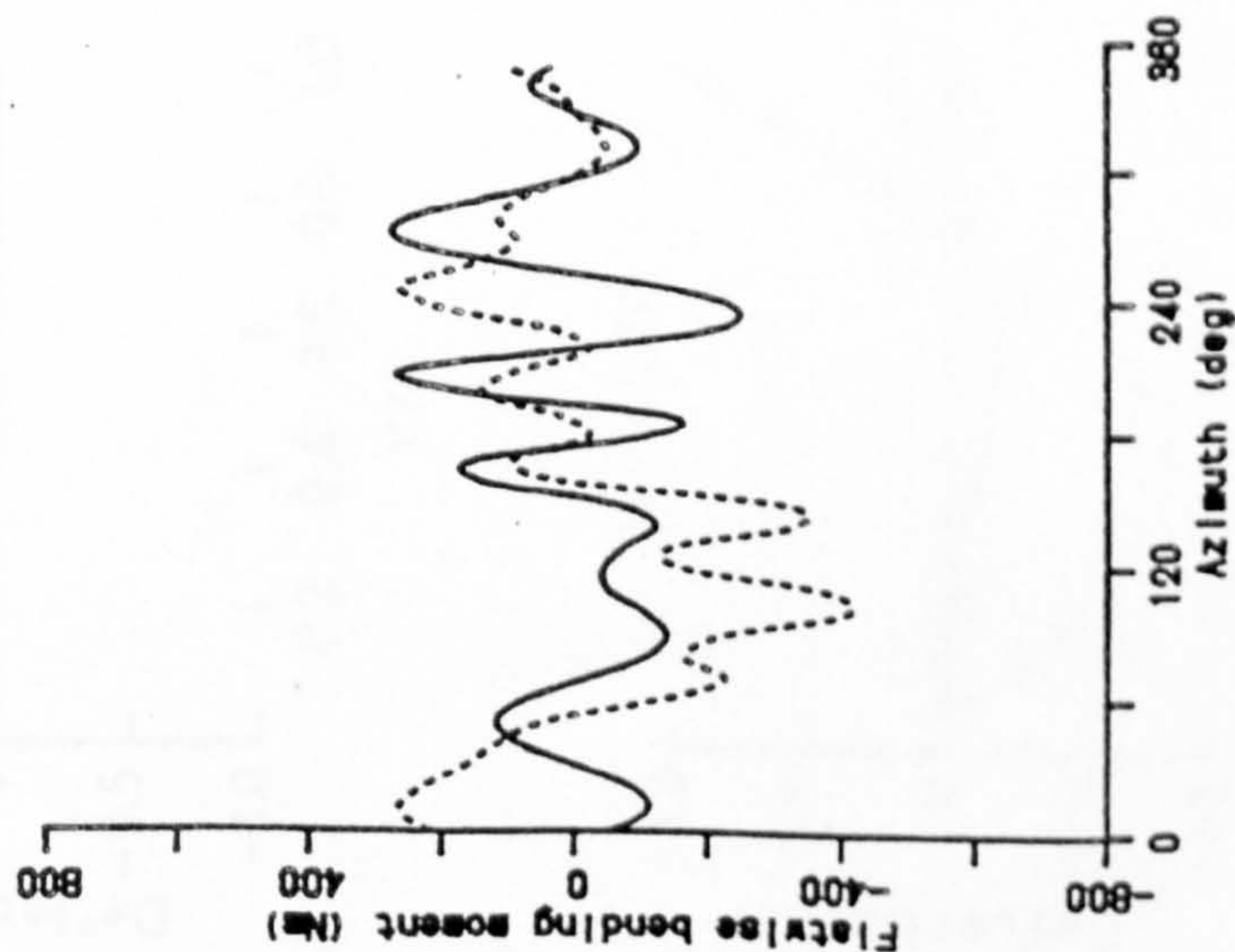
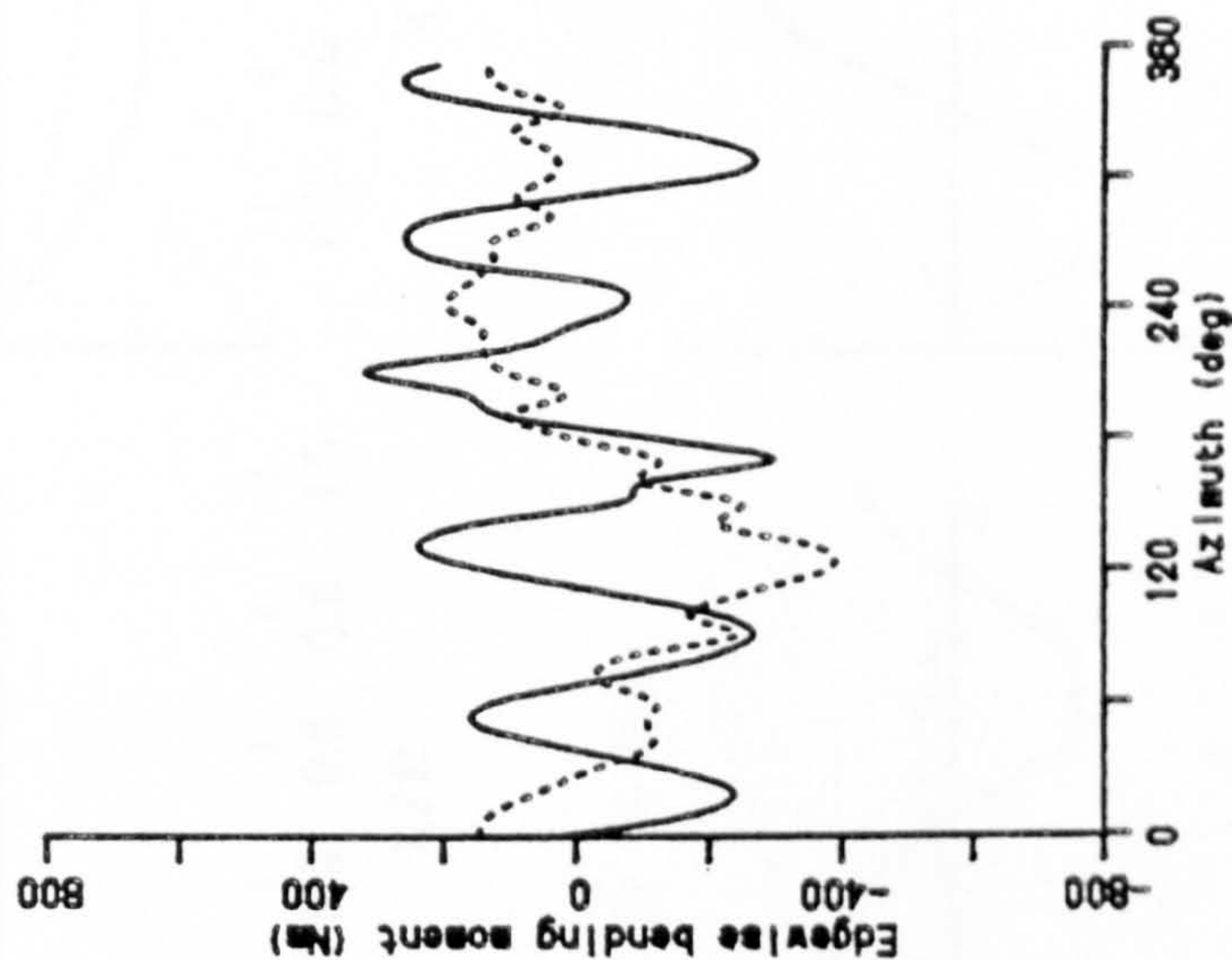
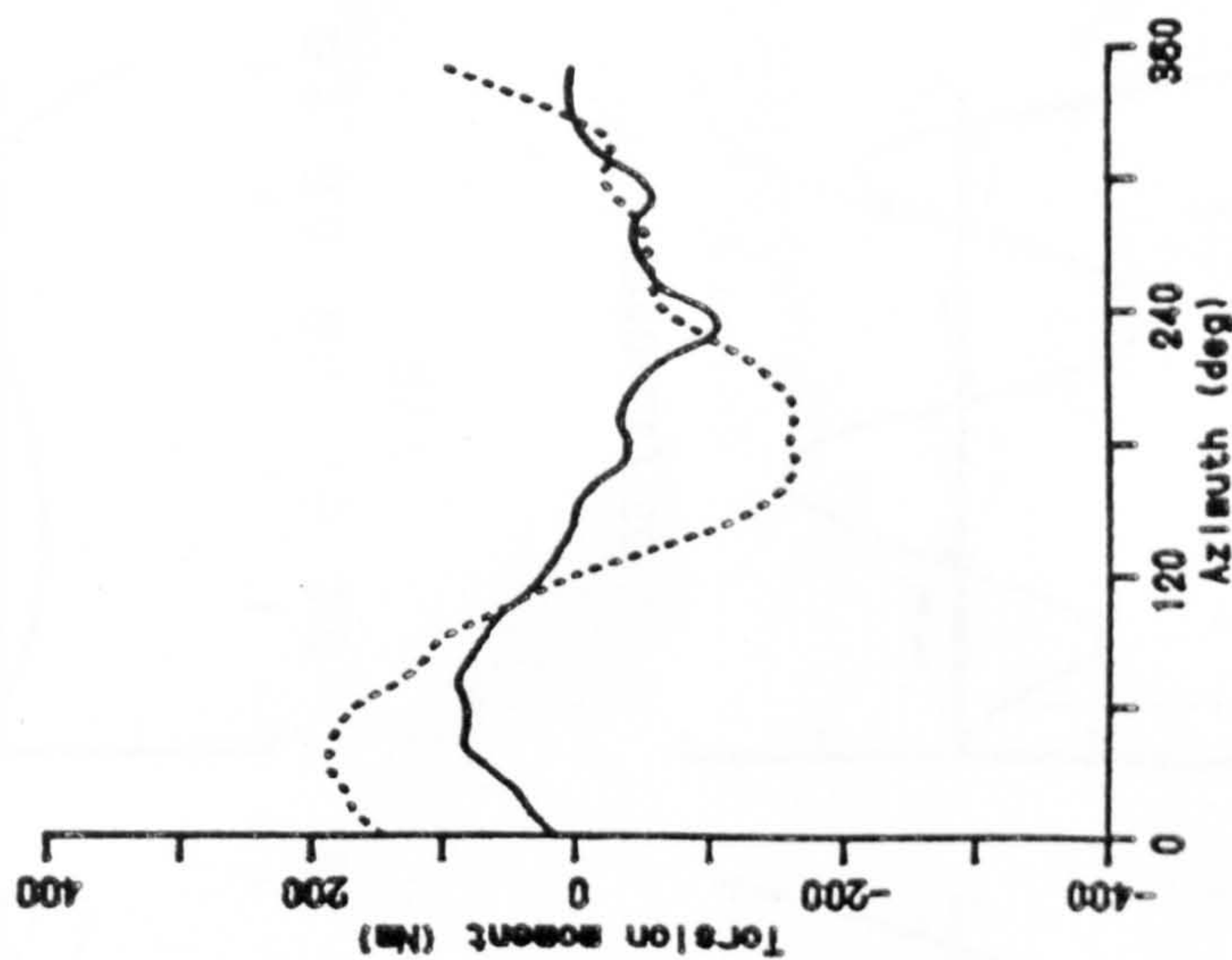


Fig 111 Bending and torsional moments on the baseline Puma blade, 18% R

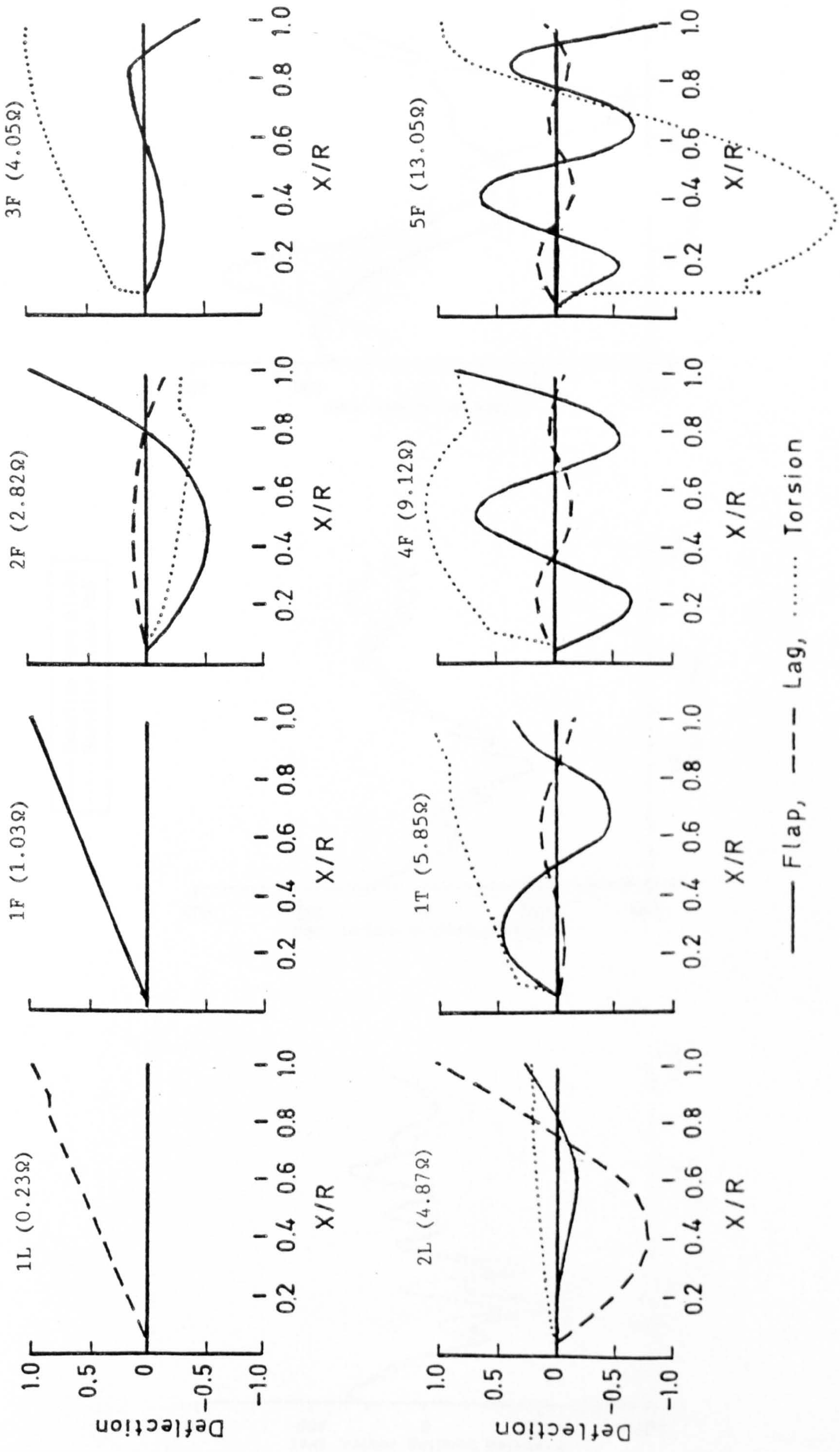


Fig 112 J134 mode shapes for a modified Puma blade (267 rev/min)

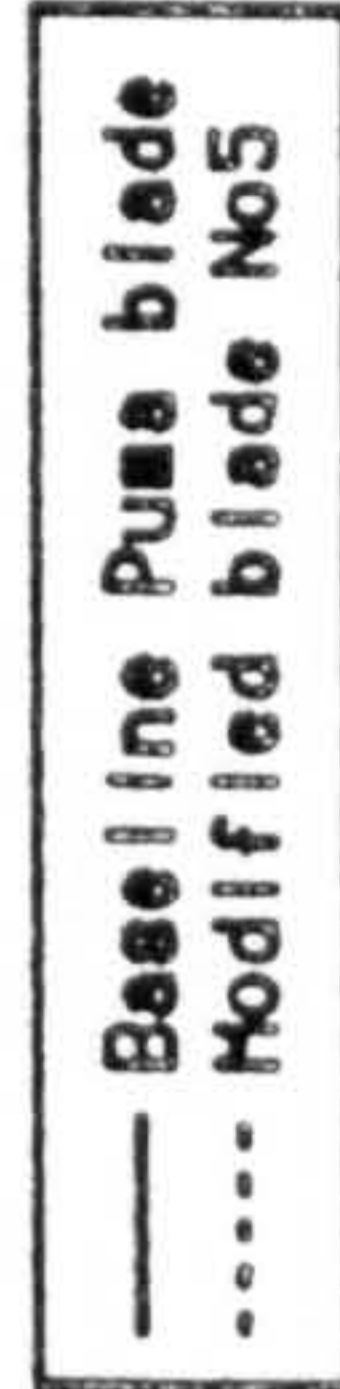
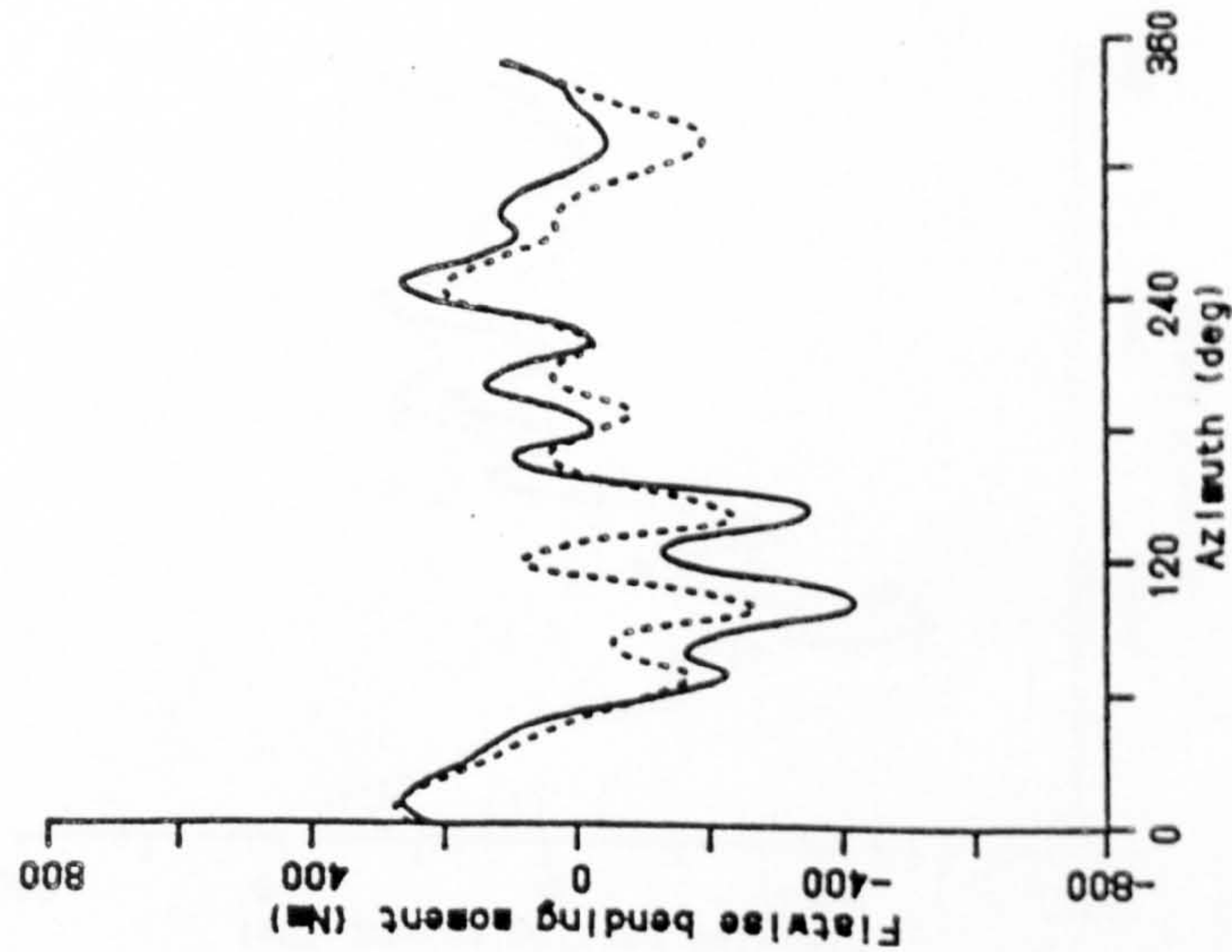
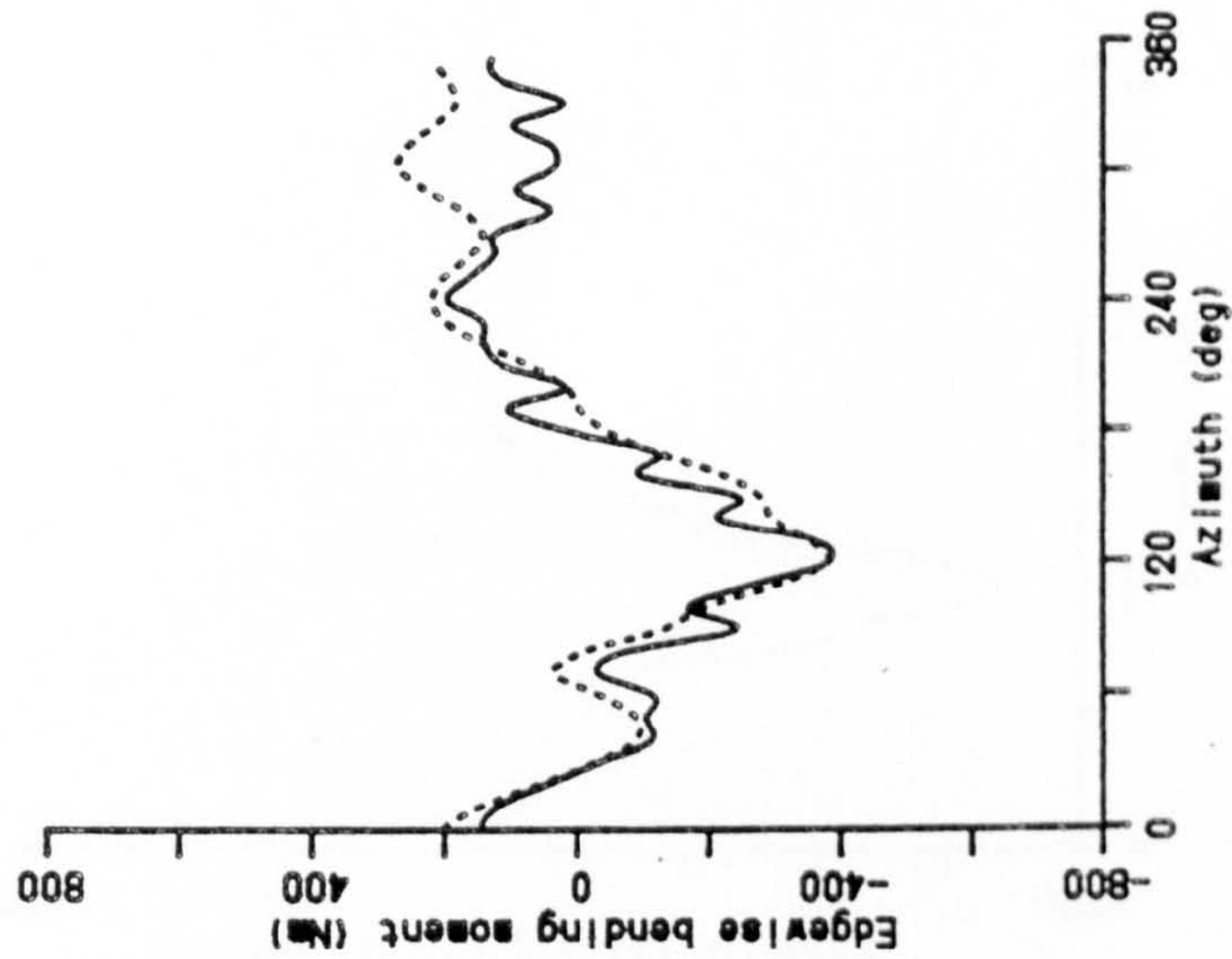
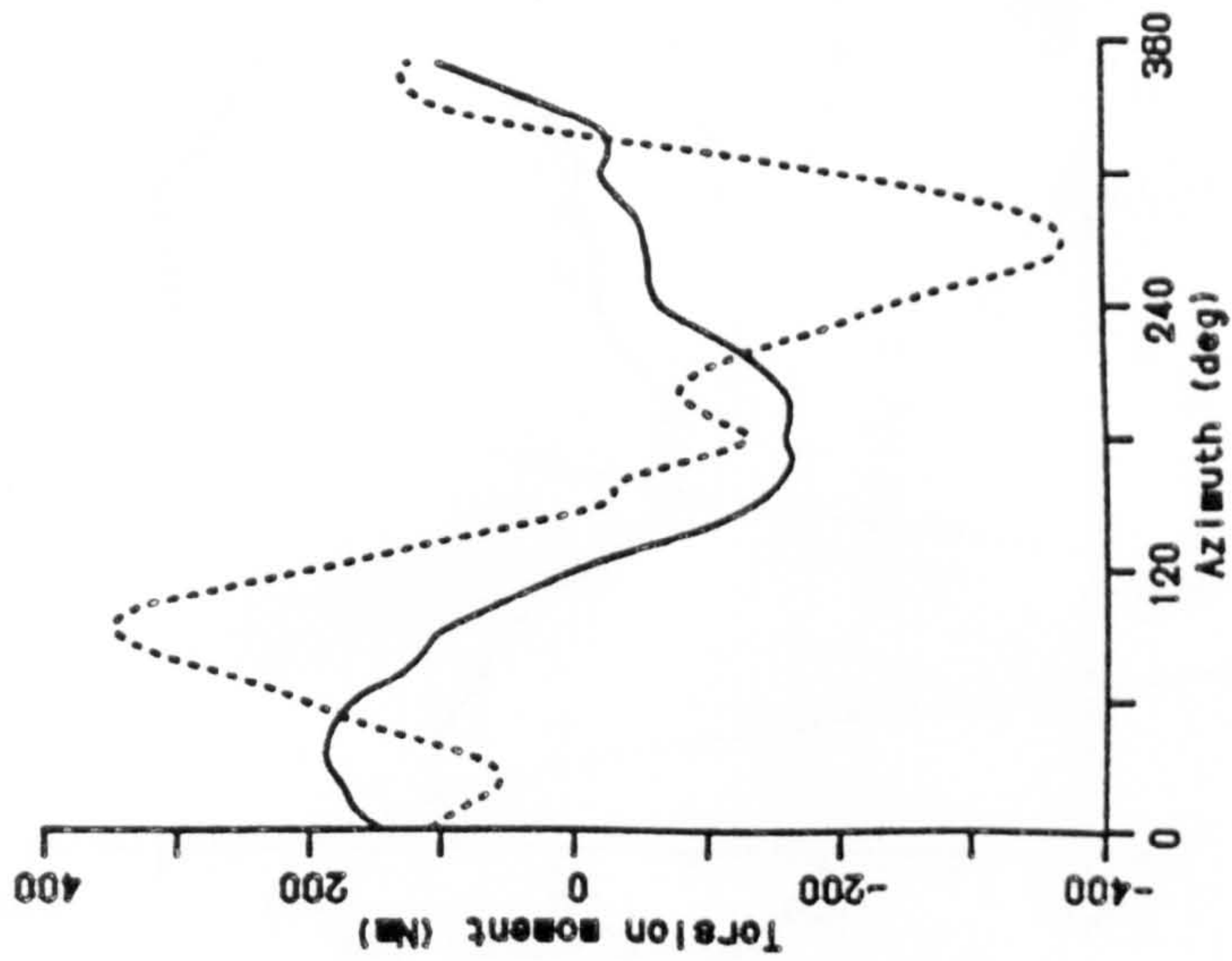


Fig 113 Bending and torsional moments on a modified Puma blade, 18% R

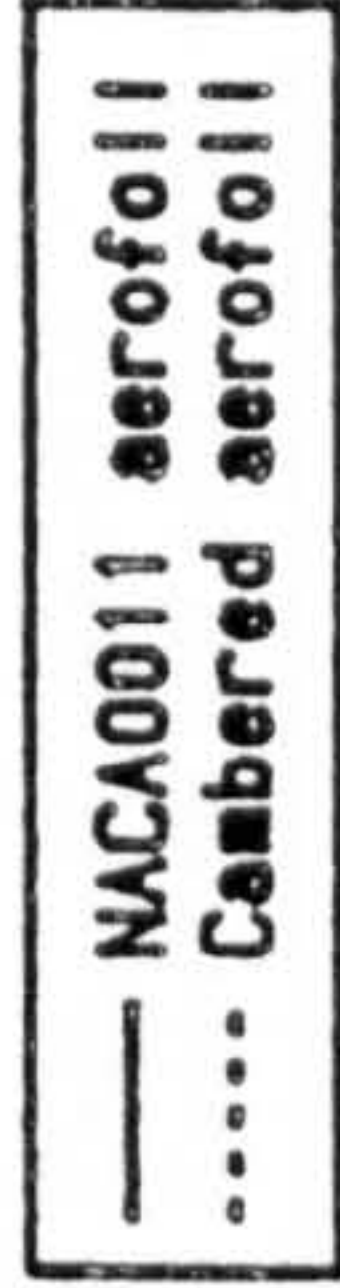
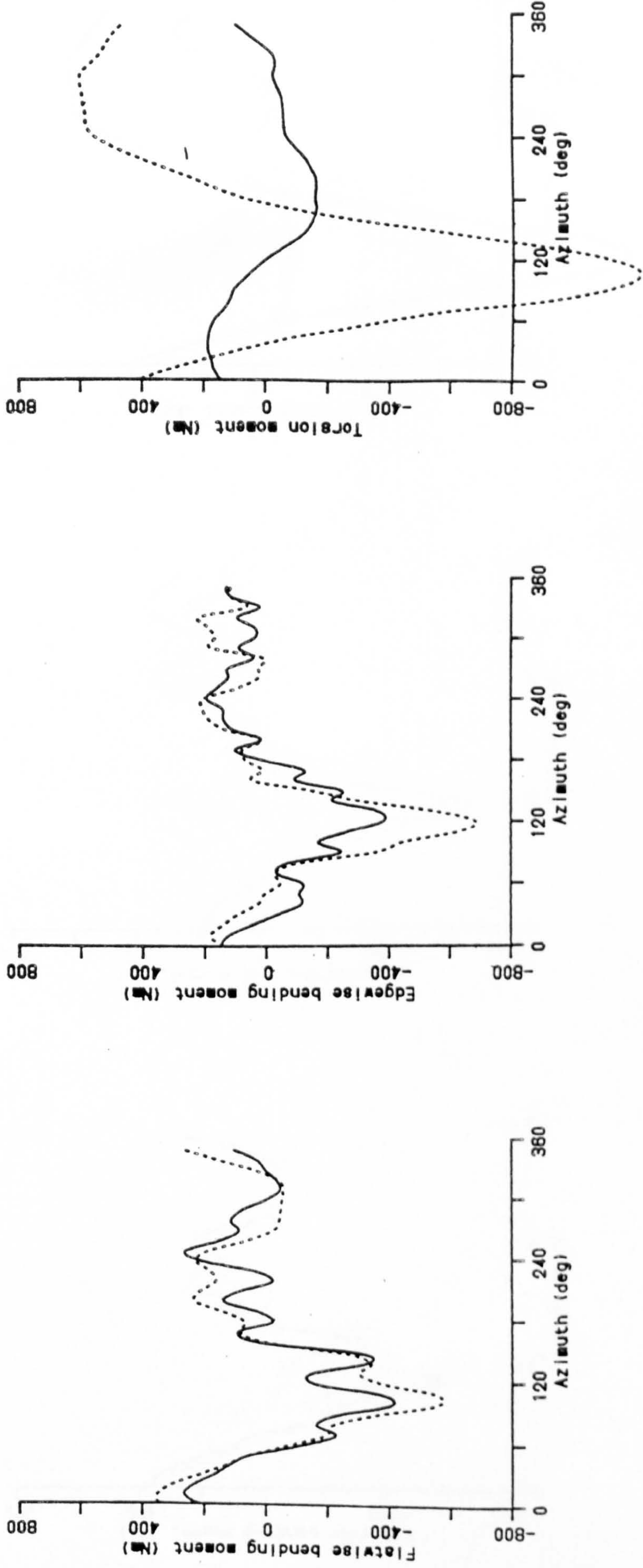


Fig 114 Bending and torsional moments on a Puma blade for cambered and symmetric aerofoil sections, 18% R

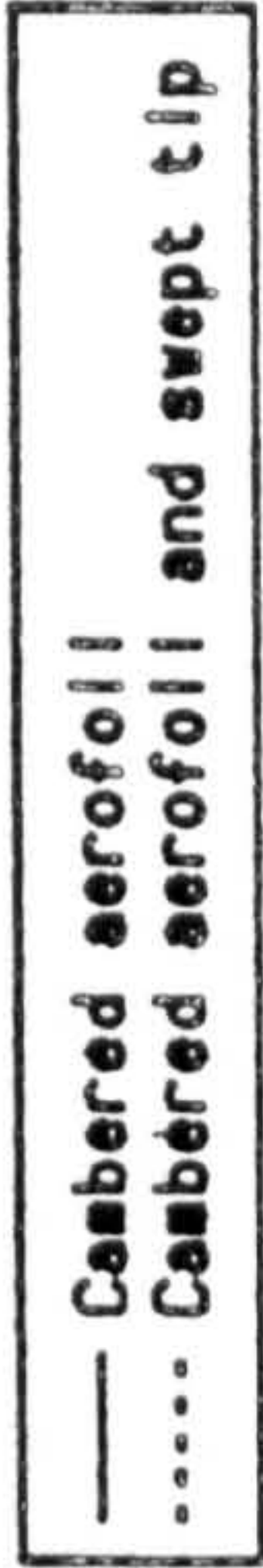
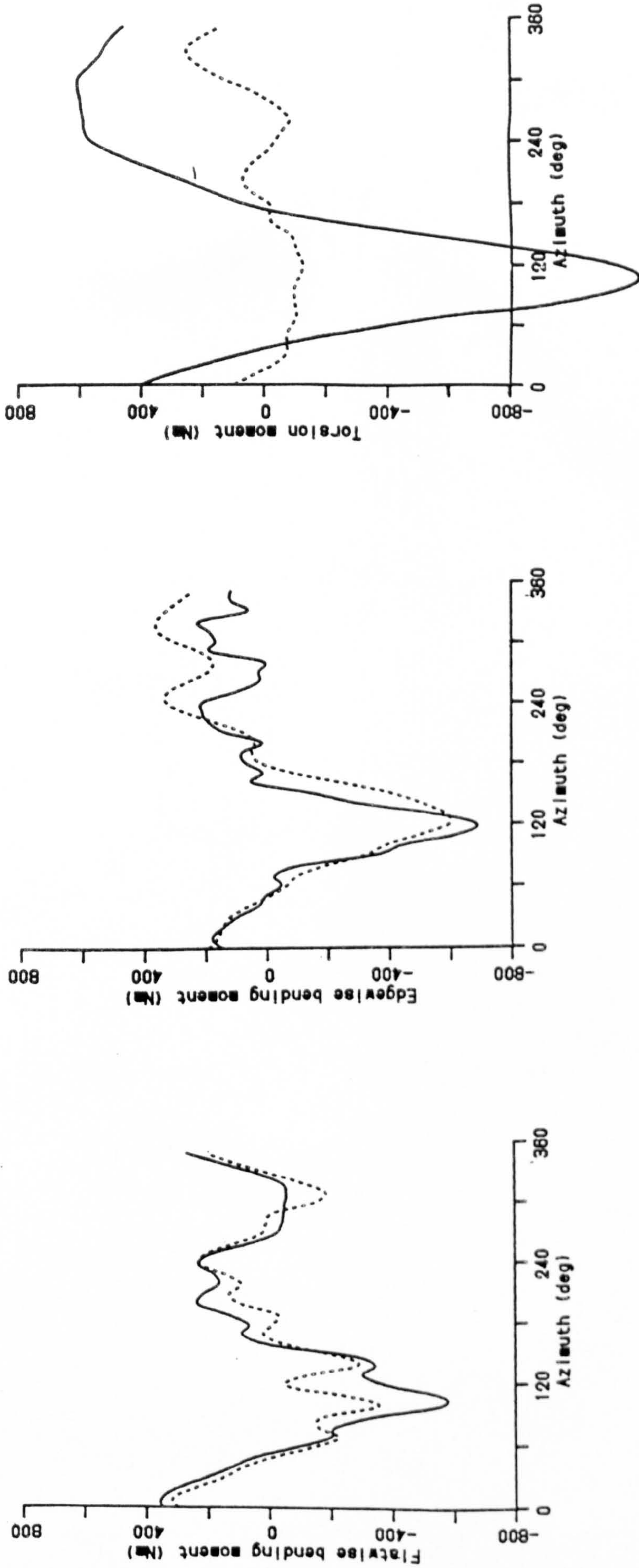


Fig 115 Bending and torsional moments on a rectangular and swept tip Puma blade with a cambered aerofoil section, 18% R