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MEASUREMENTS OF OSCILLATORY AERODYNAMIC HINGE MOMENTS FROM THE RESPONSE OF A WIND TUNNEL MODEL TO TURBULENT FLOW

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SUMMARY

Analysis of the response of a wind tunnel model to turbulence in the tunnel flow has enabled aerodynamic hinge moments to be derived. A comparison is made between results obtained by this technique and those obtained from 'steady-state' oscillatory measurements. The basic dynamic data from both sets of results are in close agreement but small differences in the data lead to rather larger differences in the values of the aerodynamic derivatives.

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* Replaces RAE Technical Report 73130 (revised) - ARC 35231.

CONTENTS

1 INTRODUCTION

Current interest in the application of digital analysis techniques to aeroelastic phenomena[1] and the need to gain experience in such techniques, have led investigators to explore applications for which adequate techniques already exist but where the digital technique may prove to have advantages. Such exploratory work not only builds up experience but offers an opportunity for a direct comparison of different techniques. An investigation of this type has been made using a wind tunnel rig designed for the measurement of oscillatory aerodynamic hinge moments on an oscillating control surface. This Report describes a method used to obtain values of the hinge moments from measurements of the response of the control surface to turbulence in the wind tunnel airflow. Digital techniques were used to analyse the response records. The results are compared with those obtained from tests on the same model using the 'steady-state'* response technique.

It is concluded that the simplification of the test rig and the reduction in wind tunnel operating time, resulting from using tunnel turbulence to excite the model, are significant advantages. There were, however, differences in the values of the hinge moment derivatives obtained from the two techniques, the differences being greater in the stiffness derivatives. The values of the derivatives are extremely sensitive to the values of natural frequency and damping of the model and small errors in these quantities can result in larger percentage errors in the derivative values. It is therefore difficult to decide which of the two sets of results is the more accurate although it is probable that greater confidence should be placed in the 'steady-state' results than in those obtained from tunnel flow excitation. Despite this conclusion, it is clear that the use of turbulence in the wind tunnel airflow to excite the model and the subsequent form of analysis of the response records yielded values of natural frequency and damping of the model very close to those obtained from the 'steady-state' tests.

2 MODEL AND TEST RIG

Tests were made on a model of a fin and rudder having the planform shown in Fig.1. The section was RAE 102 with a thickness/chord ratio of 0.06. The fin was built of solid steel (Fig.2) but, in its original form, several spanwise slots had been cut into the upper and lower surfaces to accommodate tubes for

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* 'Steady-state' is now in common usage in this context to denote conditions of constant amplitude sinusoidal excitation at a sequence of constant frequencies.

pressure plotting. These slots were filled with carbon fibre reinforced plastic, this material being chosen in order to increase the fin stiffness with minimum additional mass. The rudder consisted of two carbon fibre spars covered by thin carbon fibre skins; the internal cavities were filled with a low density rigid polyurethane foam to stabilize the skins. The front spar, which formed the leading edge of the rudder, was extended inboard of the root of the model and was attached to the support rig. The outer end of the front spar terminated in a wound carbon fibre bush bearing on a hardened steel pin in the fin. The pin and bush acted as a hinge at the tip of the rudder. The gap between the leading edge of the rudder and the trailing edge of the fin was sealed with a flexible Melinex membrane on the plane of symmetry of the aerofoil.

A photograph of the support rig is shown in Fig.3. It can be seen that the extension to the front spar of the rudder was attached to a cross-spring, the axis of which was coincident with the hinge line of the rudder. A light alloy torsion bar was mounted between the cross-spring support and an 'earth' point (which was effectively the wind tunnel structure). The torsion bar provided a spring restraint to rotation of the rudder about its hinge line and was designed to produce a rudder natural frequency of 215 Hz. This frequency was within the range in which the model could be regarded as a system with a single degree of freedom.

In the steady-state tests the rudder was excited by two electromagnetic exciters by means of which an oscillatory torque was applied to the torsion bar at the end opposite to the 'earth' point (Fig.3). In the turbulence tests the exciters were not energised but they were left connected to the model with the coils open-circuited in order to maintain the same rig characteristics as those of the earlier test. Since the exciters were fitted with non-conducting formers their attachment did not contribute significantly to the damping in the rudder rotation mode.

3 INSTRUMENTATION

Response of the system was detected by strain gauges which were bonded to the torsion bar near its mid-point. The gauges were oriented so that they responded to twist of the bar and the output signal of the gauge bridge was therefore proportional to the angle of rotation of the rudder.

4 TEST CONDITIONS

Tests were conducted over a range of Mach numbers from $M = 0.6$ to $M = 1.12$ and at a Reynolds number of 2.3×10^6 based on the mean chord of the fin and rudder.

5 ANALYSIS PROCEDURES

5.1 Steady-state tests

The output signals of the strain gauges were taken to a transfer function analyser (the RAE resonance test equipment MAMA[2]) where they were analysed into components in phase and in quadrature with the sinusoidal excitation torque, the components being displayed on an x-y plotter. At each aerodynamic condition the components of the response were plotted for a range of excitation frequencies in the neighbourhood of the natural frequency. The range of values of excitation frequency was chosen so that the plotted points extended over three quarters of the characteristic 'circle' with the upper and lower limits approximately evenly spaced about the natural frequency. The amplitude of input torque was constant for each test condition. Vector plots obtained in this way were analysed by an on-line computer to give the undamped natural frequency and fraction of critical damping for the resonance[3]. The computer was also programmed to evaluate the hinge moment stiffness and damping derivatives (h_β and $h_{\dot\beta}$ respectively) from the formulae given in section 5.3.

5.2 Turbulence tests

Analysis of the response records was carried out on a digital Fourier analyser. The analyser was programmed to take in a sample of 5 seconds duration which was digitised into 4096 points at equal time intervals. The power spectrum of the sample was obtained by multiplying the Fourier transform of the sample by its complex conjugate. The analyser then took in another 5-sectond sample and repeated the process until ten such samples had been analysed. The power spectra from the ten samples were then averaged in order to obtain an average power spectrum for the record. A mean autocorrelation function was derived by taking the inverse Fourier transform of the average power spectrum. It has been shown[4] that the autocorrelation function of the response of a system to a broad bandwidth random input is the sum of oscillatory exponentially decaying functions of the lag time (for positive values of lag), and that the decay rates and frequencies of oscillation are those of the free decay of the original system. In order to obtain the decay and frequency characteristics in the form of the well-known vector plot, the procedure proposed in Ref.1 of obtaining the Fourier transform of the positive lag half of the autocorrelogram was followed. The resulting vector plot was then analysed by computer[3] to yield values of f_r and μ_r for use in equations (1) and (2) below (see section 5.3).

5.3 Determination of stiffness and damping derivatives

For harmonic motion of control surfaces, the general expression for the aerodynamic hinge moment, H, is:-

$$H = \rho V^2 S c^2 (i\nu h_{\dot\beta} + h_\beta)\beta$$

where ρ, V are air density and velocity respectively

S, c are fin-and-rudder span and mean chord respectively

β is the angle of rotation of the rudder and is positive for trailing-edge-down rotation

ν is the frequency parameter $= \frac{2\pi f c}{V}$

ω is the circular frequency of oscillation of the rudder

$h_{\dot\beta}$ is the aerodynamic damping derivative

h_β is the aerodynamic stiffness derivative

For the system under test, it has been shown in Ref.5 that values of h_β and $h_{\dot\beta}$ may be calculated from the formulae

$$(-h_\beta) = \frac{4\pi^2 I \left(f_r^2 - f_0^2\right)}{\rho V^2 S c^2} \tag{1}$$

$$(-h_{\dot\beta}) = \frac{4\pi I (f_r \mu_r - f_0 \mu_0)}{\rho V^2 S c^3} \tag{2}$$

where I is the moment of inertia of the system about the rudder hinge line,

f_r, f_0 are the undamped natural frequencies in the airstream and in vacuo respectively,

and μ_r, μ_0 are the fractions of critical damping in the airstream and in vacuo respectively.

The value of frequency parameter ν corresponding to values of $(-h_\beta)$ and $(-h_{\dot\beta})$ obtained from formulae (1) and (2) is taken to be

$$\nu = \frac{2\pi f_r c}{V}$$

It should be noted that values of $(-h_\beta)$ and $(-h_{\dot\beta})$ are obtained from measurements made over a range of frequency (albeit a narrow range) and the value of frequency parameter therefore varies from point to point on a vector plot. This variation is, however, very small and is ignored.

Tests were conducted in the working section of the wind tunnel to obtain values of I, f_0 and μ_0 in the above equations (1) and (2).

To determine the moment of inertia of the system, I, small known masses were attached to the rig at points close to the exciter attachment points. For each added value of mass the undamped natural frequency of the rudder was derived and the rate of change of natural frequency with added moment of inertia was established. The generalised mass was then calculated[6]. For a mode of rigid body rotation of the rudder about the hinge line, the generalised mass is equal to the moment of inertia (I) of the system. The generalised mass was also determined from the results of tests in which the inertia forces due to added masses were represented by forces applied through the electromagnetic exciters. The RAE MAMA system[2] includes a facility for making generalised mass tests in this way and typical results are shown in Fig.4. It will be noticed that negative values of added moment of inertia were applied in the tests; such values may be investigated when the added masses are represented by forces applied through electromagnetic exciters, although not, of course, when actual masses are added to the structure. The advantage of making tests with both positive and 'negative' added masses is that it increases the accuracy with which the rate of change of natural frequency with added mass can be obtained at the condition of zero added mass.

Values of f_0 and μ_0 were obtained from tests made in the wind tunnel at zero airspeed. The procedure was to determine the rudder natural frequency and damping over a range of values of air density, then to extrapolate the results to obtain values of f_0 and μ_0 at zero air density. The tests of Ref.1 had shown that the values of f_0 and μ_0 obtained in this way could vary from test to test; the variations, although small, were sufficient to cause some variation in the values of $(-h_\beta)$ and $(-h_{\dot\beta})$ calculated from equations (1) and (2). It was necessary, therefore, to eliminate as far as possible sources of error and inconsistency in the determination of f_0 and μ_0. To this end a series of tests was made with the rig mounted in a vacuum chamber. The purpose of these tests was to investigate the characteristics of the rig in temperature-controlled conditions and at values of air density which were lower than those obtainable in the wind tunnel. The tests in the vacuum chamber showed that the rig was, indeed, sensitive to temperature changes. It was also sensitive to the mounting conditions and to small changes resulting from mechanical adjustments. It proved difficult for example, after changing torsion bars, to repeat the results of previous tests under nominally identical conditions with an acceptable degree of accuracy.

As a result of these findings all the tests at zero airspeed were made in the wind tunnel with the rig conditions identical to those of the wind-on tests, and, as far as possible, under temperature-controlled condtions.

6 DISCUSSION

The frequencies and fractions of critical damping obtained by the method of analysis of section 5 are shown in Fig.5. The variation of each parameter with Mach number is given, and the results of the steady-state tests are included in the Figure. It will be seen that the values of frequency obtained from the records of response to tunnel turbulence tend to be slightly higher than those obtained from steady-state tests. The differences are small, however, and in the worst case are less than 0.6 per cent of the steady-state value. The values of damping, with the exception of a single point at $M = 1.02$, are in good agreement with those from steady-state tests, although there is rather more scatter for $M > 1$ than occurred in the steady-state results.

The stiffness and damping hinge moment derivatives are shown in Fig.6. It is seen that the differences in frequency and damping lead to much larger differences in the values of the corresponding derivatives. The value of the stiffness derivative $(-h_\beta)$ in the turbulence tests is, in the worst case, nearly one and a half times the steady state value. These discrepancies occur because $(-h_\beta)$ is directly proportional to $(f_r^2 - f_0^2)$, and f_r is only one to one-and-a-half per cent greater in value than f_0. Thus in the present tests, if $f_0 = 215$ Hz and $f_r = 218$ Hz, an error of 0.5 per cent in the measurement of f_r gives an error of 36 per cent in $(f_r^2 - f_0^2)$. Larger errors can arise at lower values of kinetic pressure where f_r/f_0 may be less than 1.01. Similarly, the differences in the values of the damping derivative $(-h_{\dot\beta})$ at any Mach number shown in Fig.6 are larger than the differences in the corresponding damping values shown in Fig.5. In this case however $(-h_{\dot\beta})$ is proportional to $(f_r\mu_r - f_0\mu_0)$, and $f_r\mu_r$ is several times the value of $f_0\mu_0$, so that the values of $(-h_{\dot\beta})$ are not so sensitive to measurement errors as are the values of $(-h_\beta)$.

Two quite different techniques of test and analysis have, therefore, been applied to the same model over a wide Mach number range (including the transonic range) and have yielded modal characteristics that are consistently within about 1 Hz of one another in frequency and 0.006 in fraction of critical damping.

Most aeroelastic engineers would feel very satisfied with this order of agreement and would probably criticise the basic method of derivative measurement for its sensitivity to errors which, in many other dynamic tests, would be regarded as extremely small.

If, however, a choice has to be made between the techniques from the point of view of accuracy, it is relevant to consider the repeatability of results. Many of the 'steady-state' test results showed good repeatability, whereas repeated analysis of the 'tunnel turbulence' response records revealed a small degree of scatter. It is also relevant to consider the effect of errors arising in the analysis processes. Analysis of the steady-state records involves separation of the real and imaginary components of the response signal relative to the force input signal. This is a straightforward process using a resolved components indicator or transfer function analyser. In contrast, the analysis of a record of response to tunnel turbulence starts with the assumption of a broad band excitation spectrum and involves three Fourier transforms and several other processes before the 'vector plot' is obtained. These processes can lead to small errors which may be of the order of the errors which are important in this particular application. On balance, it is probable that rather more confidence should be placed in the steady-state results than in those obtained from tunnel flow excitation.

As far as the actual wind tunnel tests are concerned, there is no doubt that the use of turbulence in the wind tunnel flow to excite the model results in a significant saving in test duration. At any aerodynamic flow condition the test data may be acquired in less than a minute compared with up to thirty minutes to acquire the data in a steady-state test. Some simplification of the test rig is also possible since complete reliance on tunnel flow excitation would eliminate the need for exciters on the test rig.

It has been assumed that turbulence in the tunnel flow can provide satisfactory excitation to a rig of the type used. This will not always be the case. Clearly, the level of turbulence and the aerodynamic characteristics of the model must be such that there is sufficient force input to produce an adequate level of model response at all resonance frequencies of interest. In addition, the spectrum of the input must be flat over the bandwidth of the model resonance. With a narrow bandwidth (associated with low damping) this requirement will generally be satisfied unless there is a peak in the spectrum close to the resonance frequency.

7 CONCLUSIONS

Control surface hinge moment derivatives have been evaluated from analysis of the response of a wind tunnel model to turbulence in the tunnel flow and these have been compared with derivatives obtained from 'steady-state' oscillatory measurements on the same model. The comparison shows that the basic dynamic data (natural frequency and damping) obtained from both techniques are in close agreement. However, because of the sensitivity of the value of the aerodynamic derivatives to small changes in basic dynamic data, there are rather larger differences in the derivative values from the techniques.

It is concluded that, on grounds of accuracy, the steady-state technique is to be preferred but the case for preferential choice is not strong. On grounds of test economy, the 'tunnel turbulence' technique is preferable since it results in significant reductions in test duration and enables the test rig to be simplified.

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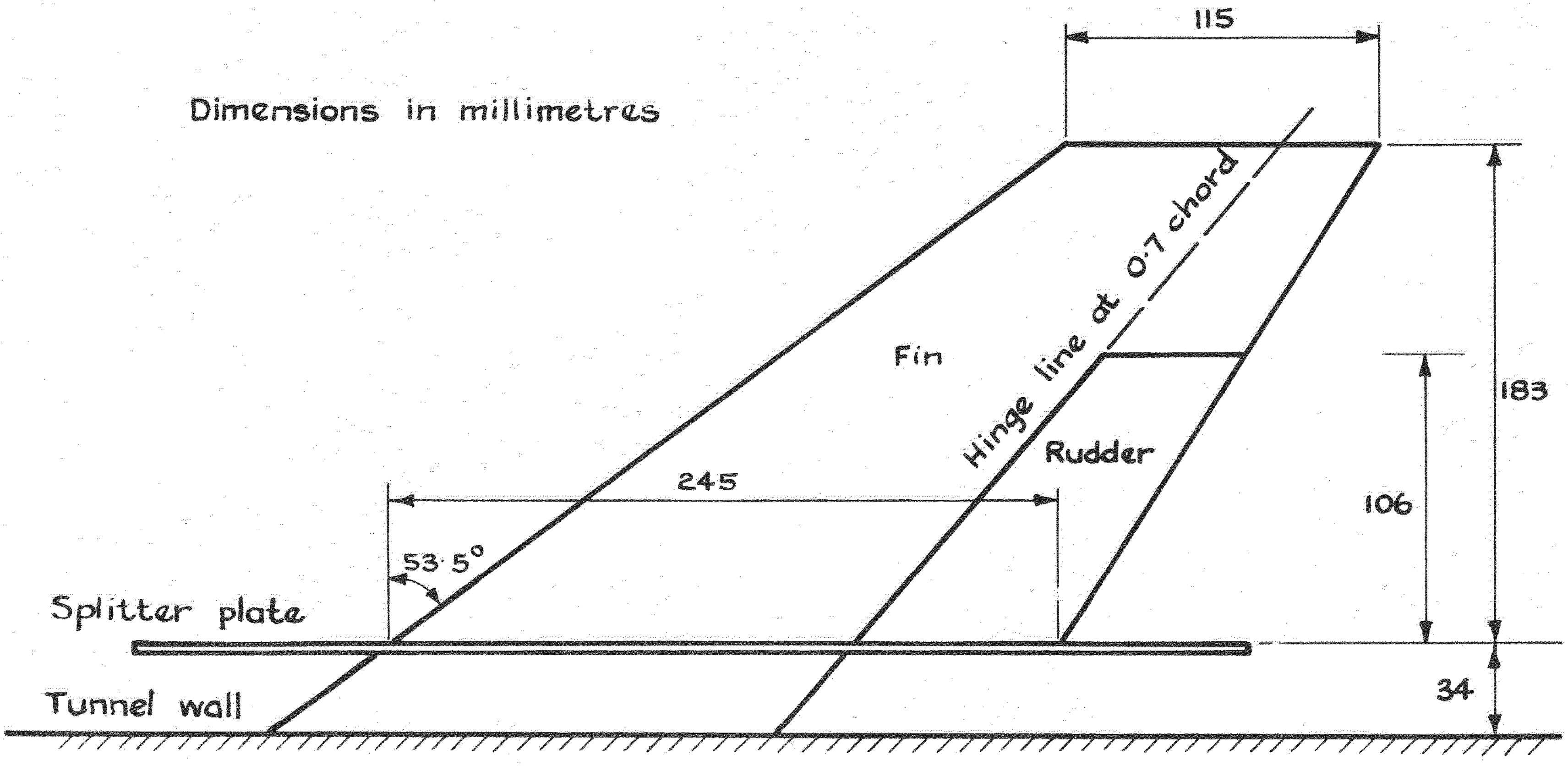


FIG. 1. Model geometry and arrangement in wind tunnel

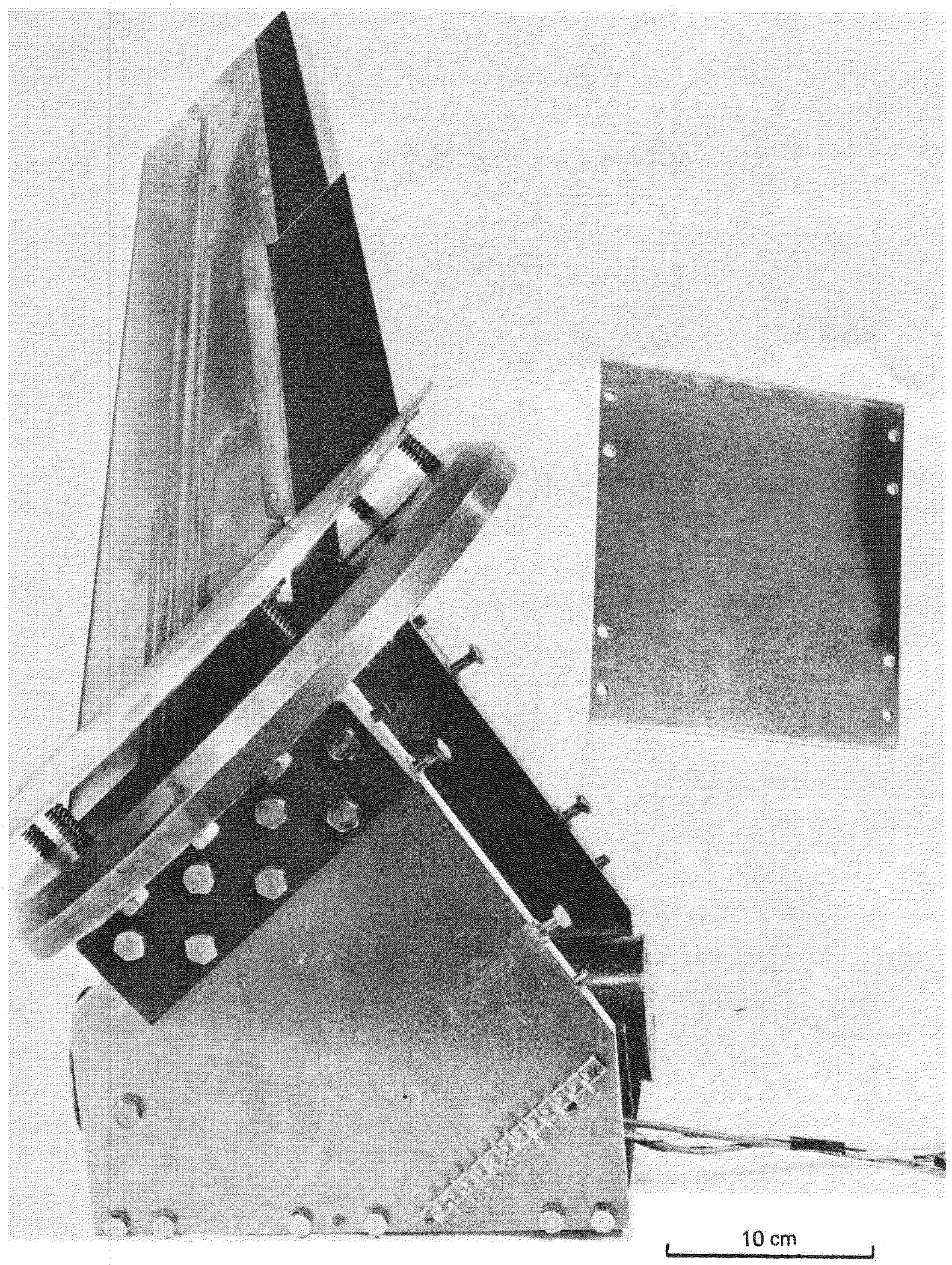


Fig.2 View of model and rig

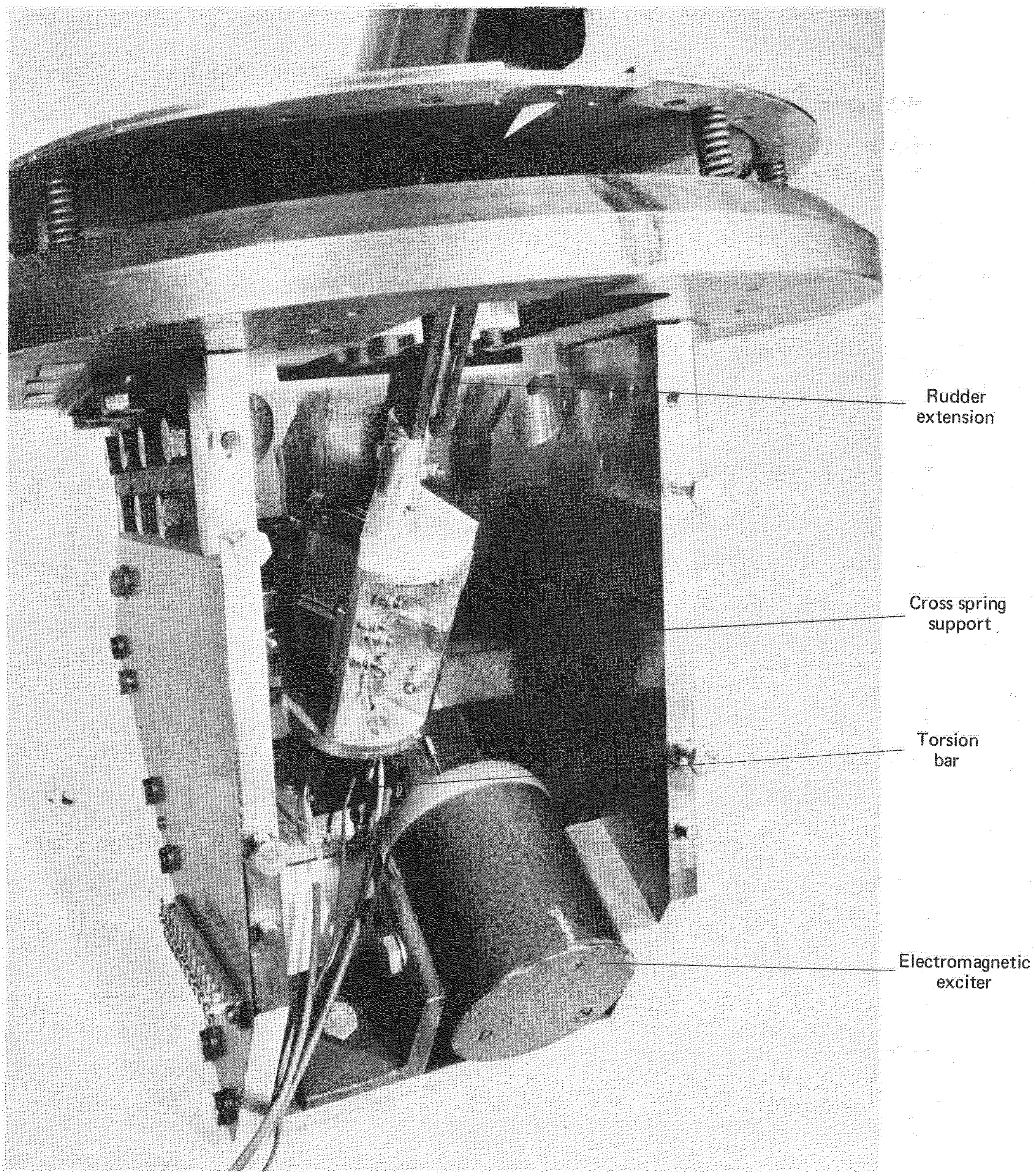
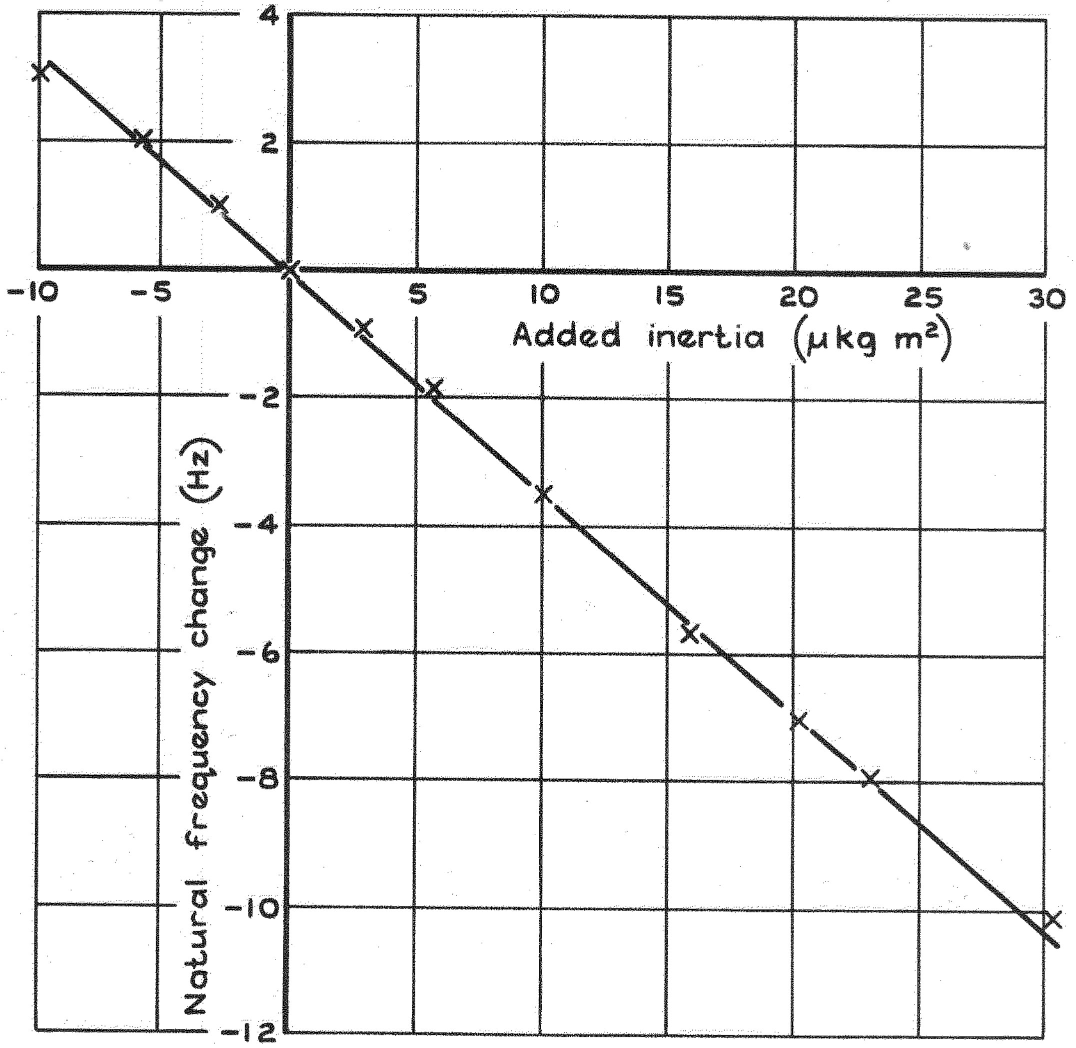


Fig.3 View of rig showing excitation system



Natural frequency with zero added inertia = 265 Hz
Rate of change of moment of inertia
with frequency = $-2{\cdot}90$ μkg m^{2}s

Fig. 4 Variation of natural frequency with rudder moment of inertia

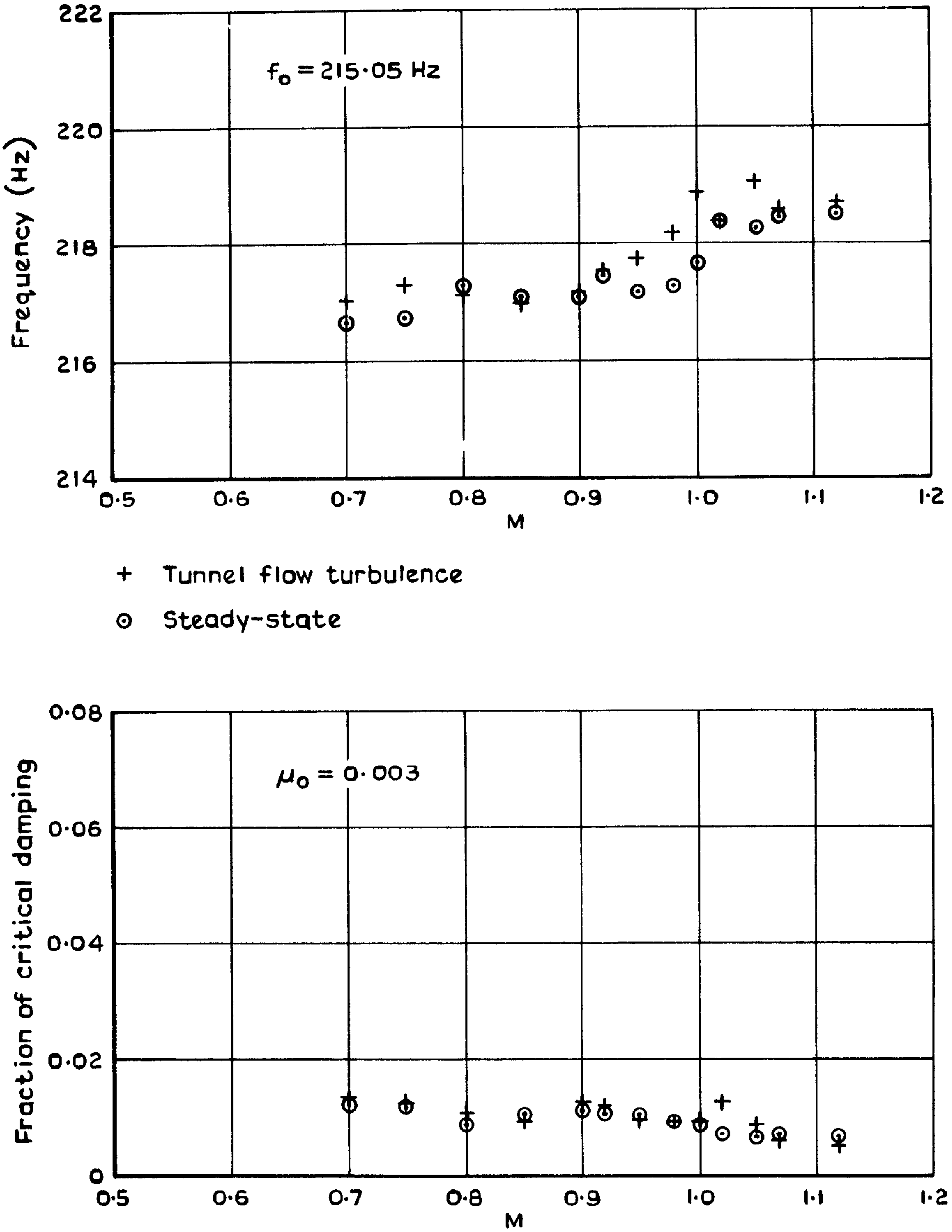


Fig. 5 Frequency and damping values

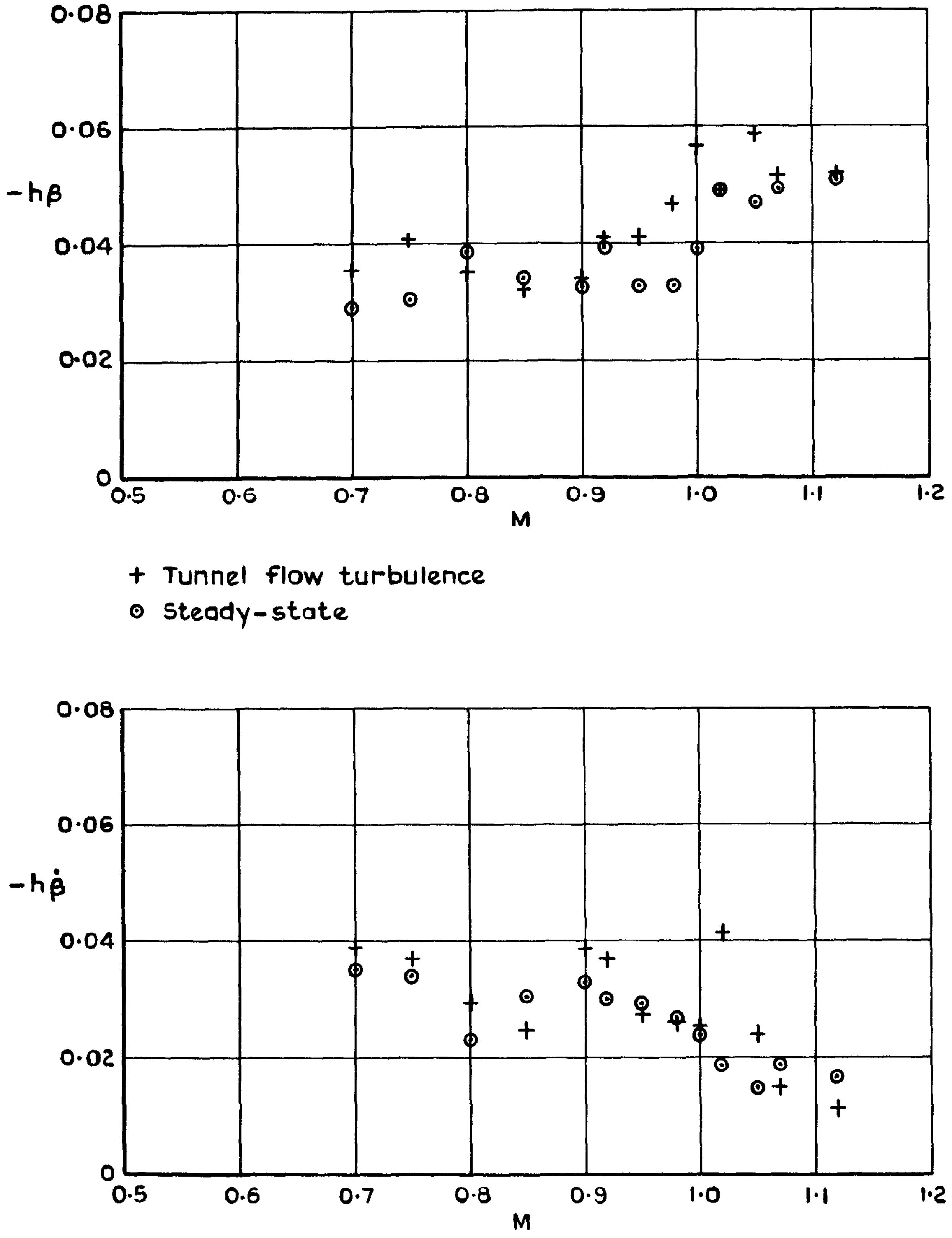


Fig. 6 Values of hinge moment derivatives

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