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The Torsional Oscillations of Airscrew Blades at Low Mean Incidences

By J. P. JONES

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The Torsional Oscillations of Airscrew Blades at Low Mean Incidences

By J. P. Jones

of the Department of Aeronautical Engineering,
University of Southampton

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Summary. An attempt is made to explain the observed torsional oscillations of an airscrew by means of the 'wake' theory. It is shown that the aerodynamic torsional damping becomes negative at certain blade angles and frequencies; the frequencies, or speeds, at which a single blade becomes unstable are shown to differ slightly from the critical frequencies of a multi-blade airscrew. At low incidences it does not appear to be possible to completely avoid this type of oscillation, but it is suggested that the oscillatory stresses might be reduced by moving the flexural axis towards the leading edge.

1. *Introduction.* Usually the problem of airscrew flutter is only serious at blade angles for which some or all of the blade is stalled, but recent tests[1] have shown that a second form of flutter can be important at small (positive and negative) blade angles. The essential features of the experimental results are as follows:

(*a*) Torsional oscillations occur in the fundamental mode at rotational speeds close to those for which the natural frequency is an integral multiple of the rotational speed. (The variation of frequency with rotational speed for a typical airscrew blade is shown in Fig. 1). The symbols 4, 5, 6 indicate the slope, in cycles per revolution, of the straight lines to which they are adjacent.

(*b*) The occurrence and amplitude of these oscillations is a function of the blade angle (Fig. 2). In the static thrust condition an oscillation at a frequency of seven times the rotational speed occurs over a very limited range of rotor speeds and incidences, but the incidence for the 6th order oscillation is much increased and in the 5th order case the range of speeds and incidences is very wide.

(*c*) The phase angle between the adjacent blades changes by 90 deg as the order of vibration changes; at a frequency of 6 cycles per revolution adjacent blades were 90 deg out of phase.

These oscillations are similar in character to the single-degree-of-freedom bending vibrations which have been observed on both model[2] and full-scale[3] helicopter blades. Since no other mode of oscillation appears to be involved, except possibly bending as a by-product of the torsion, then unless some external forcing mechanism is present it follows that the torsional damping must be negative at these speeds and frequencies (or so small that any external forces which may be present excite oscillations of considerable amplitude). In a previous paper[4] the theory was advanced that the helicopter blade oscillations were partly sustained by the presence of the vortex wake which is shed as a result of the oscillation. If the fact that a rotor blade actually passes over, and very close to, the wake it shed in previous revolutions is taken into account, it can be shown that the flexural damping becomes very small for integral values of the frequency ratio (frequency ratio = number of cycles of the oscillation per revolution). Although the same analysis showed that the aerodynamic damping of rotor-blade torsional oscillations is unaffected by the wake, it should be remembered that the flexural axis of a rotor blade is in a very different position to that of an airscrew blade. On a rotor blade the flexural axis is at the quarter-chord, whereas on an airscrew blade it is much closer to the mid-chord. Therefore, as a first step in providing an explanation, it was decided to use the wake theory to calculate the aerodynamic damping of torsional oscillations about an axis in the vicinity of mid-chord.

2. *Theory.* 2.1. *General Description.* The theory is based on the argument that the wake produced by an oscillation plays a key part in maintaining the oscillation. As the blade oscillates it sheds (due to the changing circulation) vorticity, which remains more or less stationary in space whilst the blade moves away from it along a helical path. After one revolution the blade passes over this vorticity and is disturbed by its presence, thus causing it to shed more vorticity which in turn disturbs the blade after a further revolution. Under certain circumstances, then, it might be possible for these effects to be cumulative so that an oscillation could be maintained. Obviously the effect of the vorticity depends upon its distance from the blade (*i.e.*, upon the pitch of the helix) and upon the relative signs of the vorticity cast-off after each revolution. If the vortices directly beneath the blade are all of the same sign that effects will add, but if they are of alternate signs then the total effect will be much reduced.

2.2. *The Distribution of Vorticity in the Wake.* When calculating the forces and moments on any one blade, it is necessary to know the distribution of vorticity in the wake of that blade and also in the wakes of the other blades. In Ref. 4 it was assumed that it would be sufficient to consider the oscillations of a single blade, and indeed this assumption was necessary since at that time there was no data on the phase angles between the oscillations of adjacent blades. But in the present case these phase angles are known and there is no reason for not taking the wakes of the remaining blades into account. The arrangement of the wakes of 3 blades, each spaced α apart, is shown diagrammatically in Fig. 3. If $\epsilon(r)$ is the amplitude of the free vorticity at radius r and the blade is oscillating with a circular frequency of p radn/sec, then the vorticity shed into the wake of blade 0 at this radius will be

$$\epsilon(r) \exp(ipt) . \tag{1}$$

Then along a line making an angle θ with this blade, the vorticity in the wake of blade 0 is

$$\epsilon(r) \exp i\,\{pt - p\theta/\omega\} \tag{2}$$

and if all the blades are oscillating with the same amplitude the vorticity in the wake of blade 1 is

$$\epsilon(r) \exp i\,\{pt - (p/\omega)(\theta + \alpha) + \phi\} , \tag{3}$$

where ϕ is the inter-blade phase angle. This wake, of course, is below that of blade 0. Similarly the vorticity in the wake of blade 2, at an angle θ to blade 0, is

$$\epsilon(r) \exp i\,\{pt - (p/\omega)(\theta + 2\alpha) + 2\phi\} . \tag{4}$$

At this stage we have to make use of the experimental relation between ϕ and p/ω. The experiments showed that

$$\phi = p\alpha/\omega \pm \pi/2 . \tag{5}$$

The $\pm$ sign is necessary here because the experiments did not make clear whether the phase was a lead or lag.

Substituting (5) in (3) the vorticity in the wake of blade 1 becomes

$$\epsilon(r) \exp i \{pt - p\theta/\omega \pm \pi/2\} \tag{6}$$

and from (4) the vorticity in the wake of blade 2 becomes

$$\epsilon(r) \exp i \{pt - p\theta/\omega \pm \pi\} . \tag{7}$$

If the airscrew has four blades then the vorticity in the wake of the blade 3 (not shown) becomes

$$\epsilon(r) \exp i \{pt - p\theta/\omega \pm 3\,\pi/2\} . \tag{8}$$

The next wake below the airscrew is that of blade 0, shed during the previous revolution and, provided that (5) holds, the cycle of vorticity distribution below 0 then repeats indefinitely.

We now express the frequency ratio (p/ω) as the sum of an integral part (N) and a fractional part (δ), *i.e.*, we put

$$p/\omega = N + \delta \text{ where } N = 0, 1, 2, 3 \quad \text{and } 0 \leqslant \delta \leqslant 1 . \tag{9}$$

Thus in the speed range between two speeds for which p/ω = integer, the variable is δ rather than p/ω.

If we consider the vorticity in the sheets directly below 0 then

$$\theta = 2n\pi \text{ where } n = 1, 2, 3 \tag{10}$$

and the vorticity at radius r in the nth sheet of the wake is

$$\epsilon(r) \exp i \{pt - 2n\pi(N + \delta) \pm n\pi/2\} \tag{11}$$

$$= \epsilon(r) \exp i \{pt - 2n\pi\delta \pm n\pi/2\} \tag{12}$$

$$= \epsilon(r) \exp (ipt) \exp i \{- 2n\pi(\delta \pm \tfrac{1}{4})\} . \tag{13}$$

Now in Ref. 4, where only the oscillations of a single blade were considered, it was found that the vorticity in the nth sheet below 0 was given by

$$\epsilon(r) \exp (ipt) \exp i \{- 2n\pi\delta\} . \tag{14}$$

The only difference between (13) and (14) is in the factor $\pm \frac{1}{4}$ which appears because the inter-blade phase angle is only determined to within $\pm\pi/2$. But this difference appears in a very convenient form because it is δ which is increased or diminished by 0·25. It follows therefore that the theory which was developed for a single blade can be taken over directly to a four-blade airscrew provided that the results are expressed as functions of $\delta \pm \frac{1}{4}$ instead of δ (*See* 4.1 below).

2.3. *The Approximate Representation of the Wake.* As yet an exact, or even a three-dimensional, solution of the problem has not been obtained, but an estimate of the effects of the wake can be obtained by the methods of the previous paper. This is based upon the following assumptions:

(*a*) The local incidence is everywhere small (*i.e.*, the blade is small and the local inflow velocity is small compared with the velocity due to rotation), so that the pitch of the helical wake is small at all radii.

(79193) A*

(*b*) The radius of curvature of any blade element is large compared with the chord, hence the element may be assumed to move along a straight path for many chord lengths. It follows from this assumption and (*a*) that, over a distance of many chord lengths, the wake can be regarded as plane.

(*c*) The blade is of large aspect ratio; therefore finite span effects can be ignored.

These assumptions restrict the application of the theory to those blade elements between 0·6 and 0·9 tip radius, but since most of the forces are developed in this region the error is not likely to be large. Therefore we may treat the flow past any blade element between these limits as being two-dimensional. It has been shown above that the results for a single blade can be applied to a four-blade airscrew. Therefore, if we neglect the mutual interference of the blades and treat the flow as two-dimensional, the theoretical 'model' of the flow will be as shown in Fig. 4.

Here the plane of the airscrew is assumed to be horizontal; a vortex sheet is leaving the trailing edge of a blade element and beneath this is an infinite number of vortex sheets extending to infinity ahead and to the rear. The vertical spacing of these sheets ($hc/2$) is a function of the local incidence.

Using this model it is possible to calculate all the forces and moments on the oscillating blade by means of classical theory but, for the reasons given in the Introduction, only the aerodynamic pitching (*i.e.*, torsional) damping is calculated here. The details of the calculation are omitted (they are described in Ref. 4), but the results obtained are fully discussed in the next Section.

3. *The Calculation of* $M_{\dot{\theta}}$. If the axis about which the blade twists is distant kc from the leading edge, then wake theory gives the following expression for the damping derivative $M_{\dot{\theta}}$:

$$\begin{aligned} -8M_{\dot{\theta}}/\pi\rho c^3 V = & -1 - 2[1-2k][P - Q\omega'(3/2-2k)] \\ & + 2[1-2(1-2k)][(PG+QF)/\omega' + 3(PF-QG)/2 - 2k(F-G)] \\ & + 3J_1(\omega')[PY' + QX']/2 \\ & + 2[J_1(\omega')/\omega'][QY' - PX'][1-\omega'^2(3/2-2k)(3/4-2k)]\,, \end{aligned} \tag{15}$$

where $\omega' = pc/2V = pc/2\omega r = (N+\delta)c/2r$ is the reduced frequency at radius r.

The functions F and G are the real and imaginary components respectively of Theodorson's function $C(i\omega')$, and therefore both vary with ω'.

The functions P, Q, X', Y', although they also vary with ω', are primarily dependent upon the fractional part (δ) of the frequency ratio.

It is found that

$$X' = \frac{\pi\omega'\{X[e^{\omega' h}\cos 2\pi\delta - 1] + Y e^{\omega' h}\sin 2\pi\delta\}}{[e^{\omega' h}\cos 2\pi\delta - 1]^2 + [e^{\omega' h}\sin 2\pi\delta]^2} \tag{16}$$

$$Y' = \frac{\pi\omega'\{Y[e^{\omega' h}\cos 2\pi\delta - 1] - X e^{\omega' h}\sin 2\pi\delta\}}{[e^{\omega' h}\cos 2\pi\delta - 1]^2 + [e^{\omega' h}\sin 2\pi\delta]^2} \tag{17}$$

$$P = \frac{1 + X'J_0 + Y'J_1}{[1 + X'J_0 + Y'J_1]^2 + [Y'J_0 - X'J_1]^2} \tag{18}$$

$$Q = \frac{-[Y'J_0 - X'J_1]}{[1 + X'J_0 + Y'J_1]^2 + [Y'J_0 - X'J_1]^2}\,, \tag{19}$$

where $hc/2$ is the vertical spacing of the vortex sheets and where $J_0(\omega')$, $J_1(\omega')$, $I_0(\omega')$, $I_1(\omega')$ are Bessel functions and modified Bessel functions of the first kind and

$$X + iY = C(i\omega')I_0(i\omega') + [1 - C(i\omega')]I_1(i\omega') . \tag{20}$$

X, Y, F, G are all functions which arise in classical oscillating aerofoil theory; their values for the required range of ω' are given in Fig. 5. Although X' and Y', and therefore P and Q, vary most rapidly and significantly with δ, they also depend upon the integral part (N) of the frequency ratio through ω'. Thus it will be seen that there are several variables in the expression for $M_{\dot{\theta}}$, three of which, N, δ and ω', are inter-connected. On an actual airscrew blade the incidence, relative velocity and reduced frequency vary along the span and therefore the overall aerodynamic moments must be obtained from strip theory. This makes the calculations very laborious and, of course, the results obtained apply only to one particular case. Also, at this stage, the main object is to establish that the wake theory can in fact be used to account for the observed vibrations, so that the most important variables are the frequency ratio ($N + \delta$) and the incidence. Therefore for the present it will be assumed that the incidence, relative velocity and reduced frequency have representative mean values over the span. For the relative velocity and the chord it is probably sufficient to take the values at 0·75 tip radius; the incidence (i.e., $hc/2$) will be varied over the range to be expected on an airscrew.

As the frequency ratio varies then in general both N and δ vary, but it will be seen from equations (16), (17), (18) and (19) that the functions X', Y', P, Q vary much more rapidly with δ than with N, which only appears because ω' is a function of N. From the test results the lowest value of N in which we are interested is 5 and, since $0 < \delta < 1$, the maximum variation in ω' as δ varies is twenty per cent. This is much less than the spanwise variation, but in addition (*see* Section 4 below) it is found that the important range of δ is $0 < \delta < 0{\cdot}3$, i.e., ω' varies by only six per cent. Therefore it will be assumed that ω' is independent of δ. This assumption, which greatly reduces the labour of calculation, effectively separates the variables N and δ. Thus if ω' is varied it must change sufficiently to correspond to a change in the order of vibration N.

Two values of ω' have been taken, *viz.*, 0·35 and 0·55 to correspond approximately to $N = 5$ and 7 respectively. Using these two values of ω', the variation of $M_{\dot{\theta}}$ with δ has been calculated for several incidences and for two positions of the flexural axis (*i.e.*, axis about which the blade twists).

When $\omega' = 0{\cdot}35$ ($N = 5$), $k = 0{\cdot}5$, it has been assumed that

(i) $h = 0$, i.e., zero local incidence

(ii) $h = 0{\cdot}286$

(iii) $h = 2{\cdot}86$.

When $\omega' = 0{\cdot}35$ ($N = 5$), $h = 0$, it has been assumed that

(i) $k = 0{\cdot}5$

(ii) $k = 0{\cdot}4375$.

When $\omega' = 0{\cdot}55$ ($N = 7$), $k = 0{\cdot}5$, it has been assumed that

(i) $h = 0{\cdot}182$

(ii) $h = 0{\cdot}91$.

4. *Discussion of the Results.* 4.1. *Axis at Mid-Chord.* The damping was first calculated for the finite incidence cases, and the range of δ for which this is negative is shown in Fig. 6. It will be seen that $0 \cdot 05 < \delta < 0 \cdot 35$ and that for a given value of ω' (and therefore of the order of vibration N), the width of the unstable range and the magnitude of the negative damping both decrease as h increases. The magnitude of the negative damping also decreases as ω' (N) increases. Therefore, if the negative aerodynamic damping is regarded as a driving force then the theory does predict two of the principal features of the experimental results, *viz.*, that the amplitude of vibration decreases as the blade angle is increased and as the rotational speed is increased. There is one difference, however, and that is that in the spinning tower tests the vibrations were observed at speeds on either side of those for which $N =$ integer. From the theory the largest negative damping occurs at small positive values of δ, i.e., at speeds lower than the experimental values. But it was shown in Section 2.2 above that for a 4-bladed airscrew the vorticity distribution, and therefore the forces and moments, should not be expressed as a function of δ, but of $\delta \pm 0 \cdot 25$.

At this stage it is not possible to say definitely whether the positive or negative sign is the correct one, but if the positive sign is taken then the critical speeds would occur mid-way between those producing integral values of the frequency ratio. For this reason it will be assumed that the negative sign should be taken. Therefore the abscissa of Fig. 6 has been given two scales and if the lower one ($-\ 0 \cdot 25$ to $+\ 0 \cdot 40$) is taken as giving the true value of δ, then the agreement between theory and experiment is much improved. The reduction in the negative damping as N increases is a function of the finite chord of the blade element. If a blade completes ten cycles in one revolution then a complete cycle of vorticity is shed over 36 deg of rotation; but if only 5 cycles are completed in the revolution the vorticity is shed over 72 deg of arc. If, at a given radius, the chord is the same in the two cases, then it follows that the distribution of vorticity, and therefore the downwash and disturbance, in the wakes beneath the blades will be very different in the two cases. This is shown diagrammatically in Fig. 7.

The sketch shows one sheet of the vortex wake moving past an oscillating blade element. The sinusoidal curves represent the variation, with distance, of the vorticity in two wakes, each supposed to be completing an integral number of cycles per revolution, but with frequency of one (N) twice that of the other ($2N$). ROS is the chord and RR_1, SS_1 intersect that part of the wake which is beneath the chord. O is the axis of rotation and the amplitude (of vorticity) beneath O are equal. It will be seen at once that the chord embraces mainly positive values of vorticity from the N wake, but a combination of both positive and negative vorticity in about equal proportions, of the $2N$ wake. It is, therefore, to be expected that the induced effects of the low frequency wake, and certainly its influence upon the moment, are greater than those of the high frequency. The difference only vanishes if the aerofoil reduces to a point (lifting line).

4.2. *Axis at 43·75 per cent Chord.* Although this investigation does reveal the possible cause of the oscillations, as it stands it offers no suggestion as to how the trouble might be eliminated. The stresses can be reduced if the internal damping of the blade is increased and the vibration can, of course, be avoided altogether if the rotational speed is chosen correctly. The only indication of a method of eliminating the regions of negative damping comes from the calculations of Ref. 4. There it was shown that the torsional damping is positive and constant for all values of ω', N and h if the axis of rotation is at the $\frac{1}{4}$-chord. This suggests that moving the axis forward from mid-chord should reduce the negative damping and might even eliminate it altogether. Some confirmation of this can be obtained from the helicopter blade vibration rest results[2]. Although the same incidence and frequency ratio range was covered, with blades of fairly low aspect ratio, no torsional oscillations were observed.

In order to test this possibility theoretically, the variation of the damping derivative with δ has been calculated for oscillations about an axis at 43·75 per cent chord from the leading edge. In order to reduce labour, and to confirm that the theory does give sensible results at zero incidences, these calculations were made with $h = 0$, i.e., for oscillations about zero mean incidence. The results are shown in Fig. 8 with the corresponding results for $k = 0 \cdot 50$ included for comparison. The peak value of the negative damping is reduced by approximately one third, and the width of the unstable range is also reduced. Since the damping of oscillations about the $\frac{1}{4}$-chord is independent of the frequency ratio, it follows that there must be axis positions for which the aerodynamic damping is just zero, at one rotational speed. But, of course, the oscillations would not be found for axis positions behind this, because of the internal damping of the blade and its root attachment. In practice, however, it may not be possible to move the axis forward very much without producing unacceptably large steady stresses.

5. *Conclusions: Further Development.* On the basis of the 'wake' theory it has been shown that torsional oscillations can be set up in an airscrew blade whenever the rotational speed is such that an approximately integral number of cycles are completed in each revolution. It is found that the amplitude of these vibrations can be reduced by:

(i) Reducing the rotational speed if the frequency is fixed; this merely increases the order of the vibration

(ii) Increasing the blade angle

(iii) Increasing the internal damping of the material

(iv) Moving the flexural axis of the blade towards the leading edge.

It is intended that the last three of these should be checked by experiment. Measurements are to be made of the damping of pitching oscillations of a blade for a range of incidences and axis positions. Moving the flexural axis provides a very useful method of confirming the general conclusions of the theory, since a definite change is predicted and the position of this axis is not interconnected with any of the other variables. A second confirmation might then be obtained from measurements of the damping on the blades of a multi-blade airscrew. If the theory is correct, the damping of a single blade should be negative at speeds which are rather lower than on the corresponding multi-blade airscrew.

Throughout the above calculations it was assumed that only torsional oscillations occur, *i.e.*, the couplings between flexure and torsion were ignored. It is intended that calculations should be carried out to determine the importance of these couplings and also to find out whether or not it is possible to stabilise the blade by the deliberate introduction of an out-of-balance mass. This point will, however, need very careful investigation since it might lead to the occurrence of classical flutter at other speeds and incidences.

Acknowledgements. The writer wishes to thank Mr. A. C. Walker of Rotol Propellers, Ltd., for bringing this problem to his notice and for the interest and enthusiasm he has shown.

NOTATION

α		Angular spacing of blades
$\epsilon(r)$		Amplitude of free vorticity at radius r
p		Circular frequency
t		Time
θ		Angular co-ordinate of a point in the wake
ω		Rotational speed (radn/sec)
V		Wind velocity at radius r
ϕ		Inter-blade phase angle
p/ω	=	$N + \delta$ Frequency ratio
N		Integral order of vibration
δ		Fractional order of vibration ($0 < \delta < 1$)
$M_{\dot{\theta}}$		Aerodynamic torsional damping derivative
c		Chord
kc		Distance of flexural axis from leading edge.

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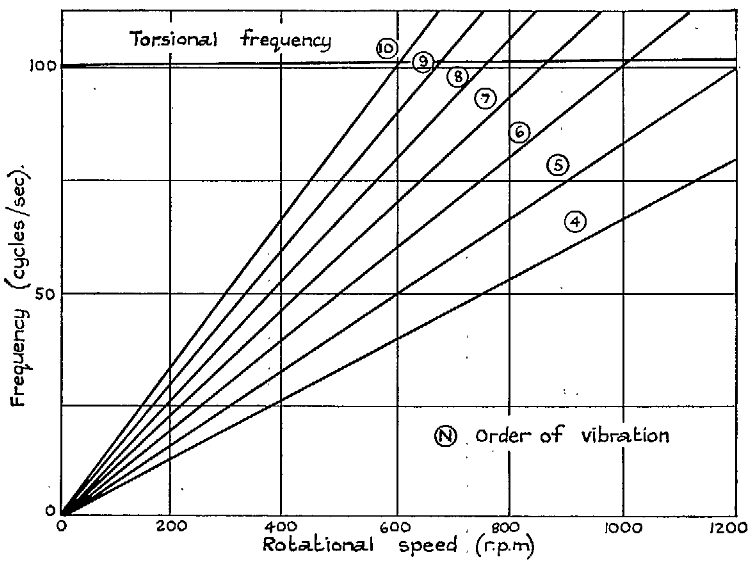

FIG. 1. Variation of the natural torsional frequency with rotational speed.

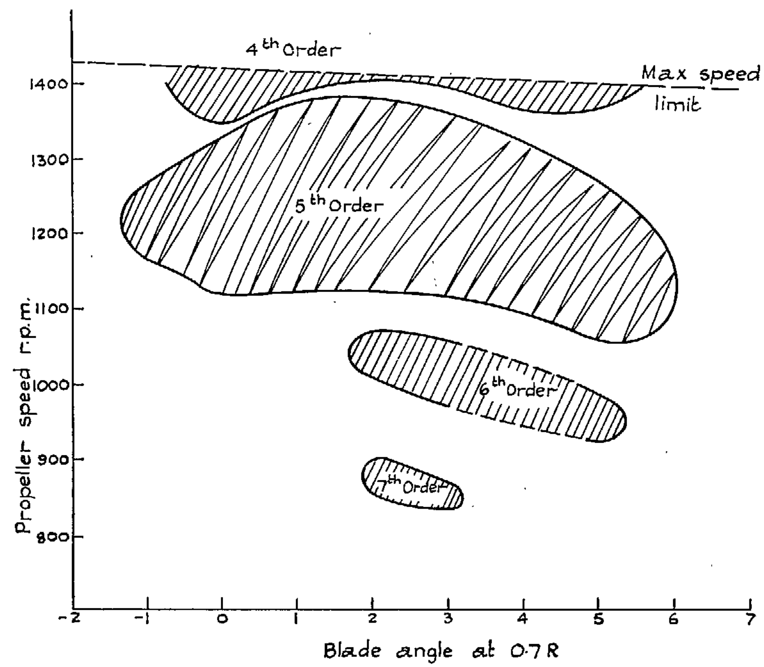

FIG. 2. Regions of torsional flutter at fine angle (reproduced from Ref. 1).

(Blade angle in degrees).

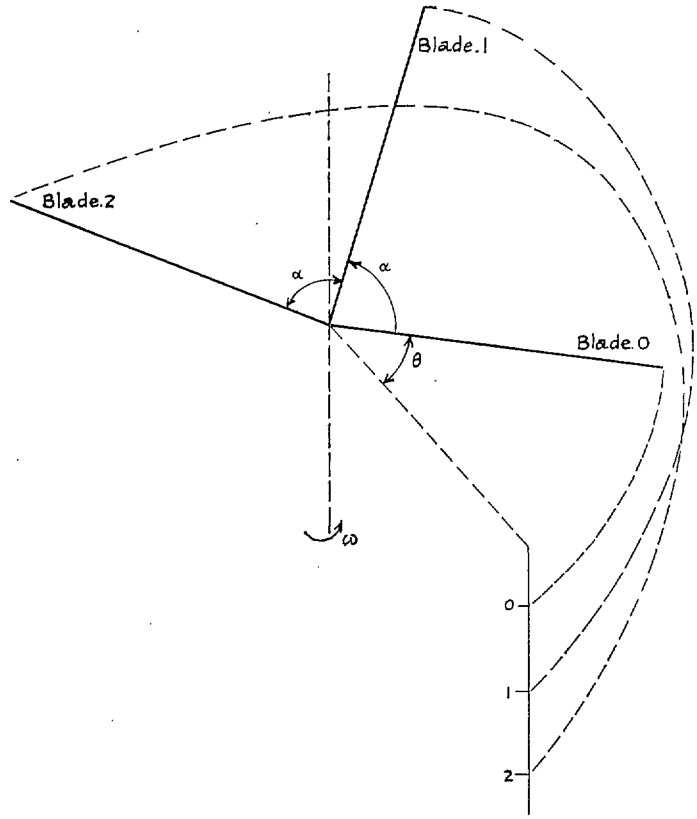

FIG. 3. Arrangement of blades and their wakes (schematic).

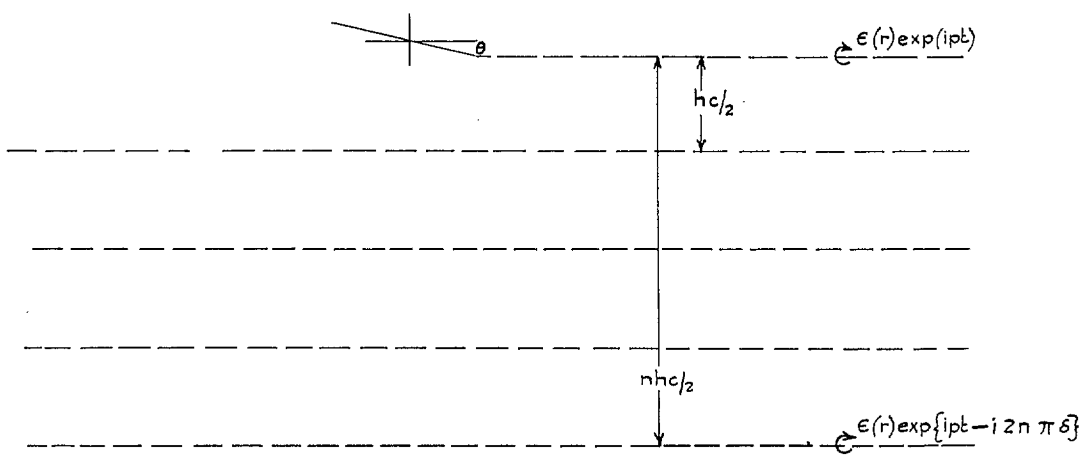

FIG. 4. Blade element and vortex sheets.

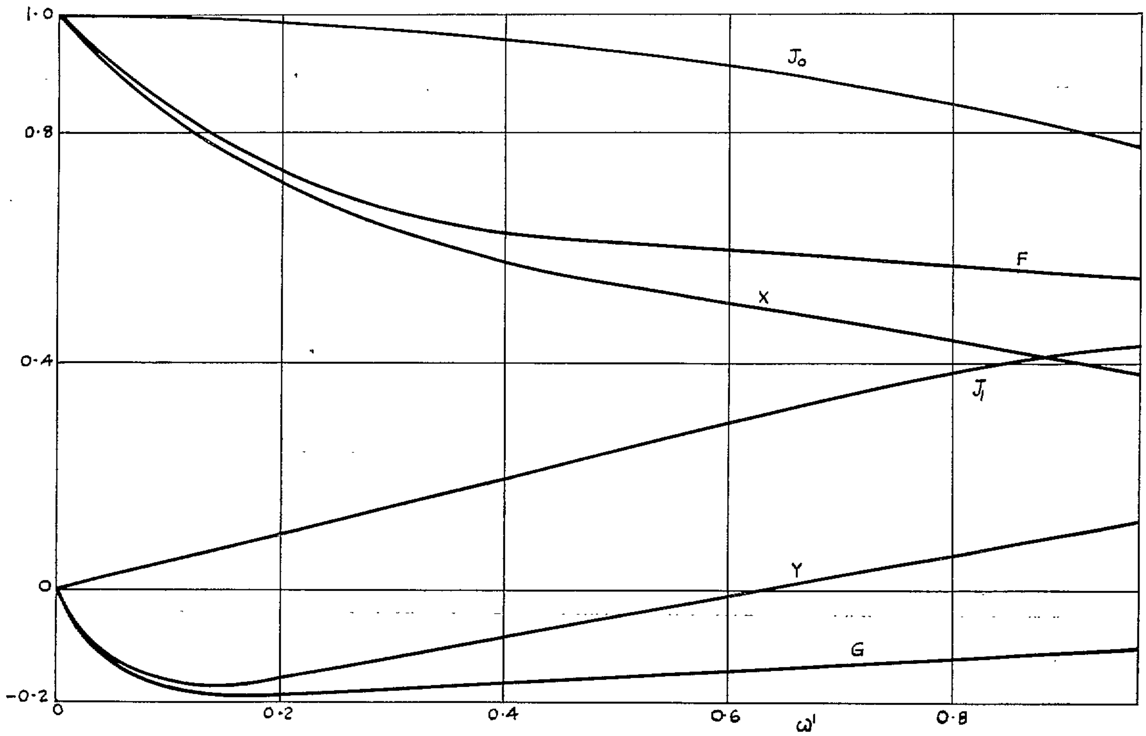

FIG. 5. Standard functions.

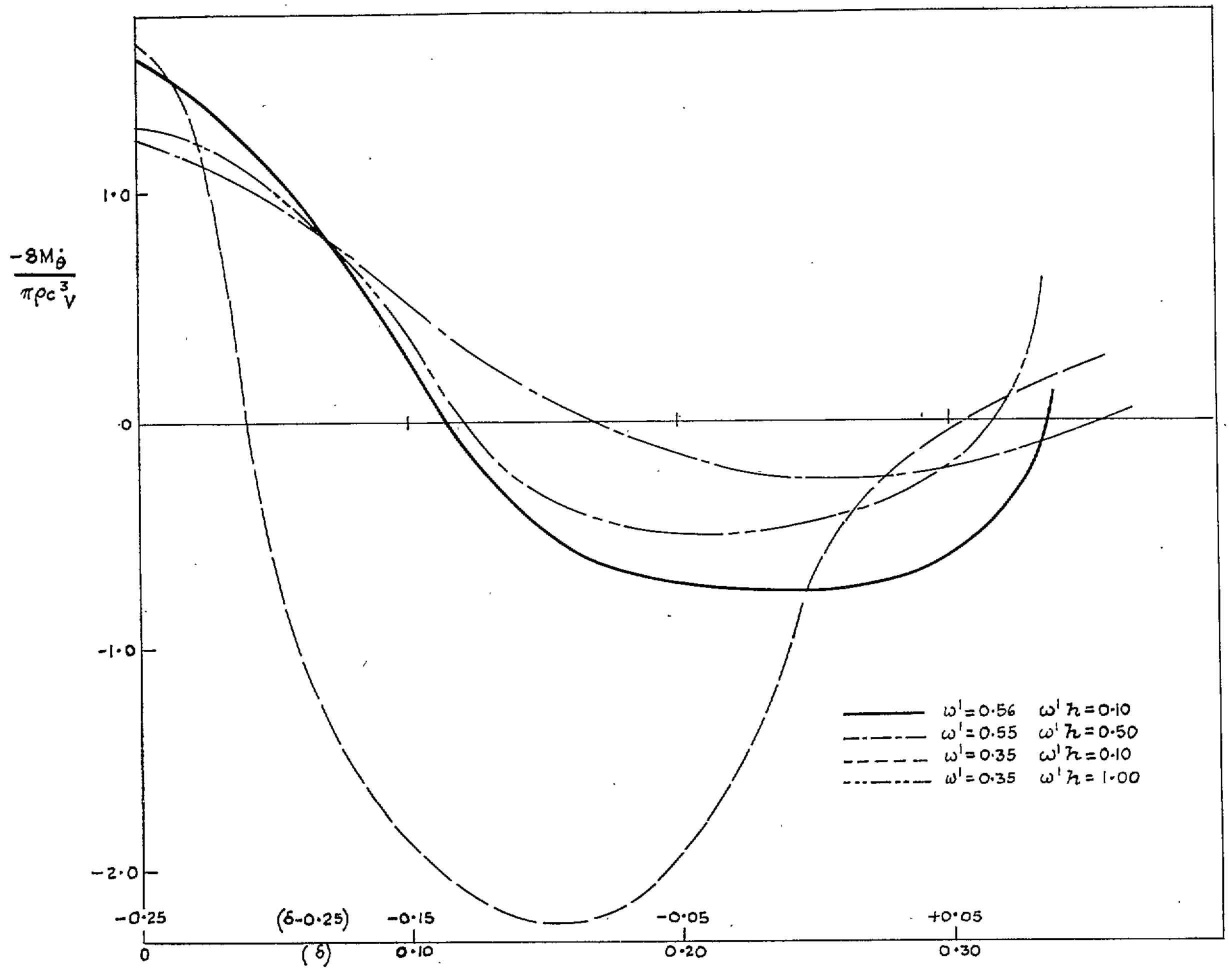

FIG. 6. Variation of the damping derivative with δ. Axis at mid-chord.

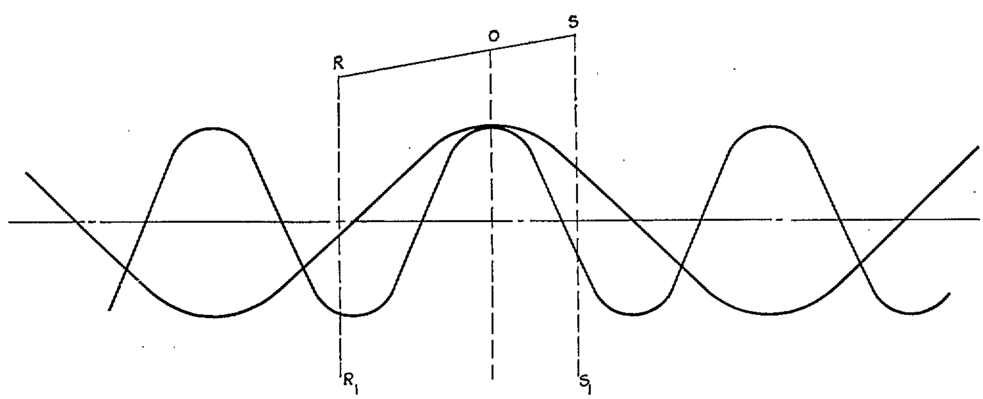

FIG. 7. Variation of vorticity below a wing of finite chord.

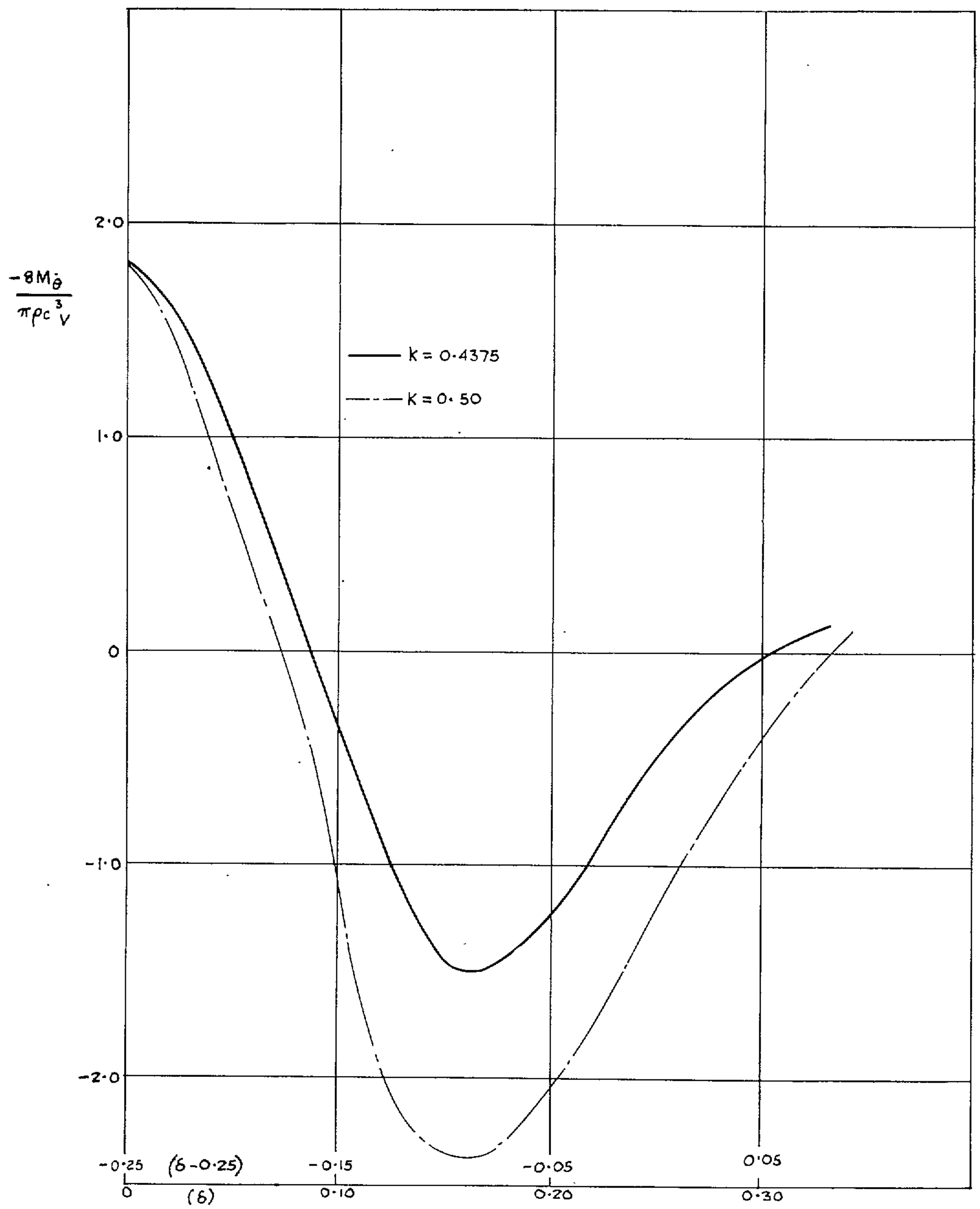

FIG. 8. Variation of damping derivative with frequency ratio.

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