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ON THE CALCULATION OF THRUST AND TORQUE FLUCTUATIONS OF PROPELLERS IN NONUNIFORM WAKE FLOW

by

Justin H. McCarthy

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LIST OF SYMBOLS

c	chord length at .7 radius
d	propeller diameter
f	frequency of oscillation
$f_b = zn$	blade frequency
$J' = \frac{V(1-w_l)}{n'd}$	local mean advance coefficient
$K_T = \frac{T}{\rho n'^2 d^4}$	instantaneous propeller thrust coefficient
$K_{T_o} = \frac{T_o}{\rho n^2 d^4}$	average propeller thrust coefficient
$K'_T = \frac{T'}{\rho n^2 d^4}$	thrust coefficient at a local section
$K_Q = \frac{Q}{\rho n'^2 d^5}$	instantaneous propeller torque coefficient
$K_{Q_o} = \frac{Q_o}{\rho n^2 d^5}$	average propeller torque coefficient
$K'_Q = \frac{Q'}{\rho n^2 d^5}$	torque coefficient at a local section
n	propeller revolutions
$n' = n\left(1 + \frac{V w_t}{\pi n d}\right)$	propeller revolutions with tangential wake correction
Q	instantaneous propeller torque
Q_o	average propeller torque
Q'	torque at a local section
r	radius to any blade section
R	propeller tip radius
T	instantaneous propeller thrust
T_o	average propeller thrust
T'	thrust at a local blade section
U	resultant inflow velocity at .7 radius
V	ship speed
w_l	local longitudinal wake fraction

w_t	local tangential wake fraction
$\bar{w}_l$	weighted mean longitudinal wake fraction from root to tip for any angular position
$\bar{w}_t$	weighted mean tangential wake fraction from root to tip for any angular position
$(1-\bar{w}_l)_m$	propeller average volume mean wake
$(1-\bar{w}_l)_T$	propeller effective thrust wake
x	nondimensional radius fraction
x_o	nondimensional root radius fraction
β	advance angle of blade section
ρ	mass density
$\nu = 2\pi m Z$	frequency of oscillation in radians/sec.
$\omega = \frac{\nu c}{2U}$	reduced frequency
θ	angular position in plane of propeller, measured clockwise from upper vertical position $(\theta = 0^\circ)$, looking forward
Z	number of blades

ABSTRACT

The general phenomenon of propeller thrust and torque fluctuations in the behind condition is discussed. Mention is made of published methods of calculating these fluctuations and of some experimentally measured values. Following, the effects of wake distribution, number of blades, propeller skew, load distribution, and stern arrangement are considered in relation to instantaneous thrust and torque absorbed by a propeller. A short method for the calculation of quasi-steady-state propeller thrust and torque fluctuations is then outlined. The method of calculation is applied to several examples and the results are compared with those given by other methods of calculation and with experimental results. Comparison indicates that the short method presented predicts thrust and torque fluctuations which are in generally good agreement with experimental results for the cases examined.

INTRODUCTION

The wake at a ship's propeller is usually defined by giving its time-average axial, tangential, and radial components in the plane of the propeller. For propellers operating in the behind condition these wake components can vary both circumferentially and radially. When a propeller operates in such a variable wake the lift forces on the propeller blades will be continuously changing, giving rise to fluctuating thrust forces and torque moments which are transmitted by the propeller shafting to the ship. In addition to fluctuating thrust and torque there are, generally speaking, other forces and moments also transmitted by the propeller through the shafting and through the water to the ship. These are: fluctuating bending couples transmitted by the propeller shafting, caused by changes in the center of effort of thrust; and waterborne pressure fluctuations acting on a ship's stern, arising from the changing pressure field about each blade as the propeller rotates. This report, however, considers only the fluctuating thrust and torque transmitted by the propeller through the shafting.

In recent years there has been a tendency to build ships of higher speeds and of greater displacements. Both of these factors have increased the required horsepower outputs of ship machinery units. As a result of this higher powering, the magnitudes of thrust and torque fluctuations have increased and have led to serious shaft-transmitted vibrations aboard some ships.

In an effort to be able to choose the optimum stern plus propeller combination for a given ship design, where the magnitudes of thrust and torque fluctuations are held to a minimum, several investigators have proposed calculation methods for predicting these fluctuations. None of the methods which have been proposed, however, proceed from a complete three-dimensional unsteady theory, which takes into account the time-dependent distribution along helical sheets of the vortices shed from a propeller operating in a nonuniform wake. The published methods, which may be loosely classified as unsteady methods,[1,2*] apply results from two-dimensional unsteady airfoil theory and are, therefore, approximate solutions of the propeller problem.

Other investigators have, as an approximation, neglected unsteady effects altogether and calculated fluctuating thrust and torque using a quasi-steady-state approach.[3,4,5,6] This approach assumes that a blade section in a circumferentially varying wake will develop a lift at a position in the wake which would be equal to the steady lift developed if this instantaneous wake were uniform circumferentially. In this case the distribution of the shed vortices in the propeller's slipstream is considered to be independent of time.

It is important that both the prediction of the approximate unsteady and quasi-steady methods which have been published be checked against experimental results to determine their accuracy. It is always possible

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* References are listed on page 21.

that in many cases unsteady effects are secondary and that they may be neglected in calculating fluctuations of propeller thrust and torque.

In a comparison of the predictions given by each analysis method, Breslin and Ritger[1] found that the quasi-steady-state method[3] gave thrust and torque fluctuations which were two to three times higher than those given by their approximate unsteady theory. Unfortunately, until recently, there have been little experimental data with which to compare the two methods of calculation. In this report, some of the recently published experimental measurements[7 thru 13] are discussed in relation to the predictions given by the two approaches. In addition, a short quasi-steady-state analysis method is presented which appears to give reasonable predictions, and which will permit quick calculation of thrust and torque fluctuations.

BACKGROUND MATERIAL

In designing a propeller to work in a variable wake, it is customary to neglect the effects of radial and tangential wake components. The radial component is neglected because practicable means are not available for treating it, and the tangential component is neglected because its effect is considered minor, since over the entire propeller disc for a symmetrical ship its average value is zero. Generally, they are both of small magnitude as compared to the longitudinal component of wake. In practice, it has been possible to design good wake-adapted propellers by considering only the longitudinal wake, where this component is considered uniform circumferentially, varying only with radial distance from the propeller hub. Unsteady effects are not considered.

When one attempts, on the other hand, to determine the thrust and torque fluctuations of a propeller in a variable wake, one must be concerned with local wake conditions in order to arrive at the instantaneous thrust and torque for each angular position of the propeller blades. Approaches to the problem, therefore, have considered the local tangential wake in addition to the local longitudinal wake, but as in design procedures have not considered the radial wake component. The significance of the tangential as well as the longitudinal wake in calculating the instantaneous thrust and torque at a blade section may be readily visualized by considering its effect on the local advance angle (β) as shown in Figure 1.

Hence, the methods based on "unsteady" theory or quasi-steady-state theory have both required complete descriptions of the tangential and longitudinal wakes. With this information as a base, instantaneous thrust and torque for any angular position of the propeller blades are worked out for a given propeller whose pitch, camber, and thickness distributions are known. The quasi-steady-state method given in Reference 3 determines local thrust and torque coefficients using the Burrill calculation procedure.[14] The "unsteady" methods given in References 1 and 2 also use the Burrill calculation, except that continuously varying corrections to the coefficients are made, based on two-dimensional unsteady airfoil theory. The local section coefficients are then integrated along each blade and the total instantaneous thrust and torque of the propeller determined.

Other investigations[15,16] have, instead of using the Burrill procedure, made estimates of fluctuating thrust and torque with the Hill method.[17] There is no reason why other well-known propeller design methods such as those of Eckhardt and Morgan[18] or van Manen[19] might not also have been used. Each of the four propeller design methods mentioned has been used with success in designing propellers and all are based on the vortex theory. The primary differences between them are in the evaluation of the curved flow correction and in the criteria used for optimum circulation distribution.

Before proceeding further, however, it would be of value at this point to make a few distinctions between the conditions under which a design method may be employed successfully for general propeller design and conditions which should be met for the calculation of fluctuating thrust and torque. In designing a propeller, it is usually required that the design procedure accurately assign pitch, camber, and thickness to the blades for a <u>single</u> operating condition of the propeller. The reliability of the design method is tested by whether or not the propeller absorbs the required power at a specified RPM and speed of advance. In all cases, the design of a wake-adapted propeller assumes an average of the circumferential wake distribution at each radius.

On the other hand, in calculating fluctuating forces, where local wake conditions must be considered, there may be large deviations from the average inflow conditions assumed in the optimum design of a propeller. (This is especially true for single-screw ships). It is important, therefore, that in the use of current design methods to predict fluctuating forces, a careful examination be made to determine whether they accurately predict thrust and torque over a wide range of inflow conditions. The check may be made by working the design methods backwards, where for a given propeller, the performance in varying inflow conditions is computed by iteration.

Some work has already been done on this by Kerwin,[20] who programmed the van Manen and Eckhardt and Morgan methods for the IBM 704 computer, and with suitable corrections for drag, was able to compare the predictions given by the two methods with experimental open water curves for twenty-one Troost Series B propellers, over a wide range of uniform inflow conditions. He found "...reasonably close over-all agreement between the two. However, in many instances large discrepancies exist indicating that existing techniques are not entirely satisfactory, even in the ideal case of an essentially optimum open water propeller."

In all four design methods mentioned, attention must be given to the fact that the Goldstein correction factor, used in going from the theoretically assumed infinite number of blades to a finite number, is employed throughout. The Goldstein factor is derived for the special case of optimum circulation distribution along a blade. In the case of an arbitrary circulation distribution along a blade, which would exist for a blade in a locally varying wake, Lerbs' induction factor[21] should instead be used.

In calculating the fluctuating thrust and torque of a propeller, either exactly or by quasi-steady-state approach, using either the Goldstein factor or the induction factor, the work involved is long and tedious and it becomes necessary to resort to the high-speed computer in order to make the calculation tractable. As an alternative solution, it would be of value to consider approximate methods of calculation which may be easily worked without resort to a computer.

One of the first approximate solutions was proposed by Brehme.[4] He found that the relative effect of number of blades on the amplitude and frequency of propeller thrust and torque fluctuations could be estimated by use of propeller open water characteristic curves. By assuming a mean wake from root to tip of a blade, for each angular position in a given wake field, the local mean advance coefficients were calculated and instantaneous thrust and torque coefficients read from an open water performance chart for the propeller. Summing up the values for all blades, it was possible to construct a diagram of total instantaneous propeller thrust and torque (quasi-steady-state) versus angular position.

Later, Schuster[5] and Schuster and Walinski[6] extended this approach and gave a comprehensive mathematical basis for the relation between a given wake distribution and the resulting thrust and torque fluctuations. By harmonically analyzing the wake, it was possible to show that when the amplitudes of the wake harmonics were high at frequencies equal to integral multiples of blade frequency ($f_b = Zn, 2Zn, \ldots$), high thrust and torque fluctuations (quasi-steady-state) would result at those frequencies. For practical application of the method, it was assumed that thrust and torque coefficient varied linearly with advance coefficient, as determined from open water characteristic curves for a propeller, and that the local mean wake for a blade, could in most cases, be approximated by taking the local wake at .7R. Tangential wake, in addition to longitudinal wake, was considered.

Having generally discussed the work which has been published on the subject, it would be of value to treat in more detail some of the principal parameters involved in the calculation of thrust and torque fluctuations.

SOME SPECIFIC CONSIDERATIONS

The following factors and their relationship to instantaneous propeller thrust and torque fluctuations will be discussed:

1. Wake field and the harmonic content thereof.
2. Number of blades on the propeller.
3. Propeller blade skew.
4. Span-wise load distribution and blade area distribution.
5. Propeller-rudder interaction effects.

For a given wake distribution the most important factor in keeping thrust and torque fluctuations to a minimum is in the selection of the number of blades for the propeller. This fact has been established experimentally at the Netherlands Ship Model Basin (see References 8, 9, and 13). The tests were run for single-screw merchant ship propellers with four, five, and six blades, and various skews and span-wise loading distributions.

Theoretical work on the subject (for instance, Reference 5 or 15) shows that the amplitudes of fluctuations are directly related to the harmonic content of the wake. Since propeller thrust and torque fluctuations are periodic, it has been shown that the significant harmonics of the wake are those at frequencies which are integral multiples of blade rate. For instance, for a four-bladed propeller, the harmonic components of order other than a multiple of four will have little or no effect on the total thrust or torque variations. On the other hand, the number of blades for a propeller, from the standpoint of obtaining minimum thrust and torque variations, should be selected so that at critical blade rate frequencies the amplitudes of the harmonics of the wake are at a minimum.

For the ideal case of a propeller working in a circumferentially uniform wake, fluctuations would not occur. This would be the case in uniform flow or in the case of a propeller in steady flow behind a fully submerged body of revolution with no appendages. For the practical case of a propeller behind a single-screw surface ship the circumferential variation of wake is large, and the degree of variation is a function primarily of the shape of afterbody.

In the preceding section it was stated that in arriving at the quasi-steady-state part of the solution to the problem of calculating fluctuating thrust, either the Goldstein factor is employed, or, for the approximate solution, propeller open water characteristic curves are employed. By either method the tacit assumption is made that each blade, from root to tip, is operating in identical inflow velocities. (This would also be true for Lerbs' induction factor.) In actuality this is not true for a wake field which has an arbitrary circumferential distribution. If, however, the circumferential distribution is harmonically analyzed, since only those harmonics whose frequencies are multiples of blade rate are significant in this mathematical model, all blades will be working in identical inflow velocities. This approach has been used in References 1, 5, and 6. It should be pointed out, however, that in breaking the wake into its Fourier components and using these values as the input to the propeller blades, the assumption of linear superposition is made. The validity of the assumption that thrust and torque coefficients vary linearly with advance coefficient depends on the degree of nonuniformity of the wake. The greater the nonuniformity of the wake, the greater the error introduced in making the assumption of linear superposition.

On the basis of the mathematical model, where only wake harmonics at integral multiples of blade rate are of significance, it is not hard to visualize the effects which blade skew has on propeller thrust and torque fluctuations in a given wake. Suppose that the wake harmonics at blade rate for a given propeller and wake are as shown in Figure 2, at three representative radii of the propeller, i.e., .5R, .7R, and .9R. Assume that the harmonic components are in phase.

To consider the effect of skew, replace each propeller blade by a lifting line, which can be either straight or curved and which represents the effective line of encounter, from root to tip, of the propeller blade with the harmonics of the wake at each radius. Suppose, for a first case, that the skew of the propeller blade is such that the effective lifting line is straight. It is evident from Figure 2 that in this case the load fluctuations on the propeller would be a maximum, since the wake harmonics are in phase and a straight lifting line would pass through the maximum amplitudes of the harmonics at the same instant at each radius. If, on the other hand, skew is chosen so that the effective lifting line is curved, the load fluctuations will be reduced, since the curved lifting line is no longer in phase with the wake harmonics at each radius. The proper choice, therefore, of blade skew can be a factor in reducing the thrust and torque fluctuations of a propeller.

An additional parameter which should be discussed is span-wise load distribution along a propeller blade. In tests run for four four-bladed propellers having different design circulation distributions, van Manen and Crowley[13] found that in a given wake the four propellers yielded nearly identical thrust and torque fluctuations. Each propeller was designed for the same operating condition to give the same average total output. If the findings of these tests are generalized, it would mean that, practically speaking, for a given propeller output, the magnitude of thrust and torque fluctuations is independent of the design span-wise circulation distribution along a blade. The validity of this generalization may be investigated by separating the instantaneous thrust and torque at a section of a propeller into two parts. The first part, average thrust and torque, is related to the average inflow conditions and is produced by the section's camber and angle of attack. The second part, fluctuating thrust and torque, is related to the wake fluctuations and is produced by changes in the angle of attack of the section.

Consider two cases, as shown in Figure 3. In case (a) the blade section is under load for the average inflow conditions (β_o), the section having camber and angle of attack. In case (b) the blade section is under no load for the same average inflow conditions (β_o); the section has no camber or angle of attack. In each case, the chord length and thickness distribution is identical. If the inflow conditions are allowed to fluctuate (β_1), in order that the absolute thrust and torque fluctuations be identical, the slopes of the thrust and torque curves when plotted against $\tan\beta$ must be the same. In order to check the slopes of the thrust and torque curves for case (a) and case (b), calculations have been made according to Hill's procedure for the example given in Table 3 of his paper.[17] The results of the calculations are shown in Figure 4 for a wide range of $\tan\beta$ values.

It is seen from Figure 4 that the slopes of K'_T curves for both cases are identical, and that for the K'_Q curves the slope for case (b) is somewhat less than for case (a). This single comparison indicates that thrust fluctuation is independent of the mean load at the blade section, while torque fluctuation may be slightly reduced by decreasing the mean load developed by the section. This finding may be further enforced by examining the Troost Series open water propeller performance curves.[22] If they are interpreted as performance curves for a typical blade section, it will be seen that for different pitch ratios, for a given blade area ratio and number of blades: the K_T slopes are nearly identical, decreasing slightly with increasing pitch ratio (mean load); whereas the K_Q slopes decrease somewhat faster with decreasing pitch ratio. This would indicate that, generally speaking, thrust fluctuations tend to be reduced when the mean load carried at a section is increased, whereas torque fluctuations tend to be reduced when the mean load carried at a section is decreased. Depending, therefore, on whether thrust or torque fluctuations were critical, the designer could theoretically either increase or decrease the mean load at a blade section working in a critical wake harmonic of large amplitude. In practice, however, the designer will not have complete freedom to radically vary the mean span-wise load distribution along a blade, and could, therefore, probably not realize significant reductions in the magnitude of either thrust or torque fluctuations.

In the foregoing consideration it was assumed in case (b), where the blade section was unloaded for the average inflow conditions, that the section chord length and thickness were kept the same as in case (a), where the blade section was loaded for the average inflow conditions. From a cavitation standpoint, it is possible to reduce the chord length in case (b) since the loading is reduced. Let us now consider the effect of reduced chord length on fluctuating thrust and torque in an additional example, case (c). Case (c) is the same as case (b) except that the chord length and thickness are reduced by one-half. The resulting curves of thrust and torque coefficients versus tan β values are shown for case (c) in Figure 4.

It is seen from Figure 4 that the slopes of K'_T and K'_Q curves for case (c) are slightly less than for case (a) or case (b). This single comparison indicates that thrust and torque fluctuations may be slightly reduced by decreasing the chord length of a blade section. As before, this finding may be further enforced by examining the Troost Series open water performance curves.[22] At different blade area ratios, for a given pitch ratio and number of blades, the K'_T and K'_Q curves' slopes decrease with decreasing blade area ratio. Hence, generally speaking, both thrust and torque fluctuations tend to be reduced when the chord length is decreased. In practice, however, the designer will not have complete freedom to radically change the blade area distribution, and could, therefore, probably not realize significant reductions in the magnitude of thrust or torque fluctuations.

The last factor which will be considered here is the effect of propeller aperature clearance. It has been known for some years that propeller-excited vibrations may be reduced by providing adequate clearances for a single-screw propeller from the sternframe and rudder. Bunyan[23] cites ships whose propeller-excited vibrations were greatly reduced by small increases in clearance, and gives recommended minimum aperature clearances.

The problem of calculating the fluctuating thrust and torque of a propeller when aperature clearances are small, and propeller-rudder interaction effects are significant, is a formidable problem whose solution is not yet know. Løtveit[15] has made some experimental measurements of the effect of propeller-rudder clearance on the fluctuating torque of two model propellers. Fluctuating torques for the two propellers were measured for three cases: with the rudder removed, with the rudder-propeller clearance about 18% of the propeller diameter, and with the rudder-propeller clearance about 6% of the propeller diameter. The amplitudes of fluctuating torques were highest for the 6% clearance, and were nearly identical for the 18% clearance and the case of the rudder removed. Since only two clearances were tested, it is not possible to conclude from these tests what the minimum clearance should be in order to avoid increases in fluctuating propeller thrust and torque caused by propeller-rudder interaction. The minimum clearance in most cases will probably be less than 18% (see for instance, the recommended minimum clearances given in Reference 23). On the basis of Løtveit's tests it may be stated, however, that when the propeller-rudder clearance is sufficiently large, the effects of interaction are negligible, and may be omitted in the calculation of fluctuating thrust and torque.

A SHORT QUASI-STEADY-STATE ANALYSIS

The short quasi-steady-state method outlined below, although developed independently, is similar to the short methods which were developed by

Brehme[4] and Schuster.[5,6] It assumes that at each angular position of a propeller blade in a wake that the average inflow conditions, from root to tip, may be determined. The local inflow conditions are then used in entering the propeller's open water performance curves, in order to determine the instantaneous thrust and torque coefficients of the propeller. The main difference is in the method of calculation of the average inflow conditions, which includes consideration of the effect of blade skew. The method does not assume wake to vary linearly with instantaneous thrust and torque.

The local longitudinal wake (w_l) and tangential wake (w_t) components over a propeller disc are known. It is assumed that each can be averaged in such a way so as to give a weighted mean longitudinal wake ($\bar{w}_l$) and a weighted mean tangential wake ($\bar{w}_t$), which describe the overall flow to a blade, from root to tip, for any angular position (θ) in the wake.

Many proposals have been made on how to best calculate a mean nominal wake. In this study, the mean nominal wake which is assumed to describe the overall inflow to a blade at a given position is the elementary volume mean wake:

$$\bar{w}_l = \frac{2\int_{x_o}^{1.0} w_l \cdot x \cdot dx}{(1-x_o^2)} \qquad [1]$$

$$\bar{w}_t = \frac{2\int_{x_o}^{1.0} w_t \cdot x \cdot dx}{(1-x_o^2)} \qquad [2]$$

The advantages and disadvantages of the different calculation methods have been discussed by past investigators, yet, to date, the problem of calculating a mean nominal wake from wake survey data has not been resolved. In general, however, the volume mean nominal wake is probably the most widely accepted.[24,25]

For comparison purposes, two of the methods proposed have been investigated, and the results of the comparison are plotted in Figure 5 for three single-screw Series 60 ships having different stern lines. The longitudinal wake surveys have been taken from Reference 26, and the local wakes at the .7 radius are shown in Figure 5; $\bar{w}_l$ has been calculated for each angular position in the propeller plane in two ways:

1. Volume mean wake, calculated according to equation [1].

2. Thrust mean wake, calculated according to: $\bar{w}_1 = \frac{\int_{x_0}^{1.0} w_e \cdot dT/dx \cdot dx}{\int_{x_0}^{1.0} dT/dx \cdot dx}$

The thrust mean wake has been calculated for two different assumed design thrust-loading distributions. In one case the distribution is of the optimum type which would result for minimum energy loss, and in the other case the outer radii of the blades have been unloaded. Each of the thrust distributions used is shown in Figure 6; in addition, the equivalent weighting curve for calculating mean volume wake is also shown.

Agreement between the three curves of mean wake calculated depends, of course, on the degree of radial nonuniformity of the local wakes from root to tip of a blade at each angular position. This consideration explains the divergence of the curves which have been plotted in Figure 5. For the V-stern, except in the region of $\theta = 0^{\circ}$, there is good agreement between the three curves of weighted wake. For the Parent-stern and U-stern the agreement is good except in the region from $\theta = 120^{\circ}$ to $\theta = 180^{\circ}$. As should be expected, the thrust mean wake curves for optimum load distribution agree best with the volume mean wake curves, since the weighting factors in each calculation are not radically different (see Figure 6). It may be stated, then, that for a propeller with optimum load distribution, the results of the volume mean wake calculation will be in close agreement with the results of the thrust mean wake calculation.

The foregoing wake comparison has been based on the assumption of a propeller whose blades are assumed to be represented by straight lifting lines. For any position of a propeller blade in a wake, the effective points of encounter at all radii along the blade lie at the same angular position in the wake. As was shown in the previous section, the effective line of encounter of a blade with the wake may be either straight or curved depending upon the skew of the blade. For subsequent calculations which are based on the method outlined in this section, the effective line of encounter of the blade will be taken to lie along the centerline of the projected outline of a propeller blade, and mean volume wake will be calculated along this line for each position of a blade in the wake. Other methods of representing the effective line of encounter have been discussed by Saunders in Reference 27, and further work is required before the correct representation of the effective line of encounter is determined. It is believed, however, that the representation adopted in this study is a reasonable approximation, yielding calculated predictions of fluctuating thrust and torque which are in good agreement with experimental results.

Having determined $\bar{w}_l$ and $\bar{w}_t$, the local mean advance coefficient (J') may be determined for a blade at any angular position (θ).

$$J' = \frac{V(1 - \bar{w}_l)}{n' d} \qquad [3]$$

where

$$n' = n\left(1 + \frac{V}{\pi n d}\bar{w}_t\right) \qquad [4]$$

In equation [4] $\bar{w}_t$ will be either positive or negative depending on the direction of the tangential wake flow relative to the direction of rotation of the propeller. If flow is in the direction of propeller rotation, $\bar{w}_t$ will be negative, and if in the opposite direction, $\bar{w}_t$ will be positive.

By use of the characteristic open water performance curves for the propeller, it is possible to enter at a local J' and obtain a multiple of the instantaneous thrust coefficient, $(K_T)_j$, and torque coefficient, $(K_Q)_j$, for each blade at a given angular position (θ) in the wake (j denotes the number of the blade). If Z represents the number of blades, it is evident that:

$$(K_T)_j = \frac{K_T}{Z} \qquad [5]$$

$$(K_Q)_j = \frac{K_Q}{Z} \qquad [6]$$

Letting $(T)_j$ and $(Q)_j$ represent the total instantaneous blade thrust and torque for a single blade:

$$(T)_j = \frac{K_T}{Z} \cdot \rho (n')^2 d^4 \qquad [7]$$

$$(Q)_j = \frac{K_Q}{Z} \cdot \rho (n')^2 d^5 \qquad [8]$$

Let,

T = Total instantaneous thrust of all blades

Q = Total instantaneous torque of all blades

$$T = \frac{\rho d^4}{Z} \sum_{j=1}^{Z} \left[K_T (m')^2 \right]_j \qquad [9]$$

$$Q = \frac{\rho d^5}{Z} \sum_{j=1}^{Z} \left[K_Q (m')^2 \right]_j \qquad [10]$$

The percentage fluctuation in thrust ($\frac{T-T_o}{T_o}$) and torque ($\frac{Q-Q_o}{Q_o}$), where T_o and Q_o are the average total thrust and torque of the propeller, may now be determined

$$T_o = \rho d^4 \left[K_{T_o} m^2 \right] \qquad [11]$$

$$Q_o = \rho d^5 \left[K_{Q_o} m^2 \right] \qquad [12]$$

Hence,

$$\frac{\Delta T}{T_o} = \frac{T-T_o}{T_o} = \frac{1}{Z} \sum_{j=1}^{Z} \left[\frac{K_T}{K_{T_o}} \cdot \left(\frac{m'}{m}\right)^2 \right] - 1 \qquad [13]$$

$$\frac{\Delta Q}{Q_o} = \frac{Q-Q_o}{Q_o} = \frac{1}{Z} \sum_{j=1}^{Z} \left[\frac{K_Q}{K_{Q_o}} \cdot \left(\frac{m'}{m}\right)^2 \right] - 1 \qquad [14]$$

$\frac{\Delta T}{T_o}$ and $\frac{\Delta Q}{Q_o}$ correspond to given angular orientations of the propeller in the wake. By repeating this calculation for other positions of the propeller, it is possible to construct curves of the propeller's thrust and torque fluctuations versus angular position (θ). The curves will be periodic, repeating every $\frac{2\pi}{Z}$ degrees, and calculations need be made over only one interval.

In practical problems, it will be necessary for comparison purposes to find the harmonic components of the thrust and torque fluctuation curves. Since the only significant harmonics are the harmonics of blade frequency, where blade frequency (f_b) is given by,

$$f_b = Z m$$

the harmonics of the blade frequency will correspond to integral multiples of f_b. Hence, First harmonic corresponds to f_b

Second harmonic corresponds to $2f_b$

Third harmonic corresponds to $3f_b$ . . . and so forth.

Harmonic analysis may be performed by numerical integration (for example, see Reference 28) of the thrust and torque fluctuation curves in order to determine the Fourier coefficients, and therefore the amplitudes of the fluctuations for each harmonic of blade frequency. It is assumed that only harmonic thrust and torque outputs can excite and sustain a vibration, and that they, therefore, are the measure of the fluctuating thrust and torque characteristics of a propeller working in a variable wake.[29]

In applying the method outlined here, great care must be taken in computing the mean wake curves and in reading instantaneous thrust and torque coefficients from open water propeller characteristic curves. As is true by any method of calculation of fluctuating forces, the predicted results depend on small differences between large numbers, and in most cases the fluctuating thrusts and torques will fall within a range of only 0 to 15% of the average values.

In calculating the local advance coefficients (J') for entering the open water propeller performance curves, the calculated local volume mean wake should be adjusted by multiplying it by the ratio of the effective thrust mean wake to the average volume mean wake. This adjustment is made in order to account for the fact that uniform flow propeller performance results are being used to estimate propeller performance in heterogeneous flow.

COMPARISON OF RESULTS

Comparison with Other Calculated Results

The short quasi-steady-state method outlined in the previous section has been used to calculate the fluctuating thrust of the four-bladed propeller for the wakes of the three Series 60 sterns reported in Reference 26. The local volume mean longitudinal and tangential wakes shown in Figure 7 were calculated assuming the line of encounter of the propeller's blades with the wake to be straight, since the calculated results given in Reference 2 are based on this assumption. The average volume mean wakes, $(1-\bar{w}_1)_m$, over the entire propeller disc were calculated for the three sterns from Figure 7 and are shown in Table I. The model test effective thrust wakes, $(1-\bar{w}_1)_T$, are also given. In calculating the local advance coefficients (J') from the curves given in Figure 7, an adjustment was made by multiplying these advance coefficients by the ratio of the effective thrust mean wake to the average volume mean wake (see Table I).

The calculated quasi-steady-state predictions given in Reference 26, which are based on the Burrill method,[3,14] are given in Figure 8 along with the calculated results of the short method. The amplitude, peak to trough, of thrust fluctuation is in each case greater according to the short method. The amplitude of fluctuation for the first harmonic predicted by the short method is from 35% to 75% greater than that predicted by the Burrill method. It is interesting to note, however, that for the first harmonic each of the two methods is qualitatively in agreement. From the standpoint of minimizing the first harmonic thrust fluctuations, by either method, the order of preference for the three ships is firstly the V-stern, next the Parent-stern, and lastly the U-stern.

The results of the short method shown in Figure 7 have been calculated in two ways: first, neglecting the effect of the tangential wake (w_t) and, second, including its effect. As shown in Figure 7, the effect of including w_t is small in the case of the three sterns considered for the four-bladed propeller. For a four-bladed propeller, no matter what the angular orientation of the propeller, two blades will be working in the starboard side of the wake and two in the port side. Since the tangential wake is positive on one side and negative on the other side, its effect will tend to be cancelled. For a five-bladed propeller, the effect of tangential wake will tend to be more appreciable.

Comparison with Experimental Results

The predictions of the short method have also been compared with the experimental results for a single-screw tanker with two different stern arrangements and several propellers.[9] The experimental measurements were made at the Netherlands Ship Model Basin using their most recently developed instrumentation. Calculations have been performed for Propellers I, II, V, and VI, for the Conventional and Mariner Sterns in loaded condition. The fluctuating thrust and torque calculations for Propellers V (6 blades) and VI (4 blades) have neglected tangential wake. For Propellers I and II (5 blades), tangential wake has been included. Since a tangential wake survey was not made, estimated values have been used. The volume mean longitudinal and tangential wakes are shown in Figure 9.

In calculating the local advance coefficients (J') from the curves given in Figure 9, an adjustment was made by multiplying these advance coefficients by the ratio of estimated effective thrust mean wake to the average volume mean wake (Table II). The lines of encounter of the blades with the wake have been taken along the centerlines of the projected areas of the propeller blades. The estimated open water performance curves for the four propellers are shown in Figure 10, and have been extrapolated from the curves given in References 22 and 29. The average advance coefficients and thrust and torque coefficients are given in Table II for cases examined.

The experimental and calculated thrust and torque fluctuations for the Conventional Stern are shown in Figure 11. The agreement in the amplitude of fluctuations of the harmonics is good, except for the the thrust fluctuations of Propeller V. For each propeller, the calculated curves have been displaced $\Delta\phi$ degrees to facilitate comparison of the calculated and experimental results. As shown in Figure 11, $\Delta\phi$ varies between 0 and -5 degrees, indicating close phase agreement.

The experimental and calculated thrust and torque fluctuations for the Mariner Stern are shown in Figure 12. Agreement is good both in amplitude of fluctuation and phase, between calculated and experimental results, for Propeller VI. In the case of Propeller II, the agreement is not very good, either in amplitude or phase. The reason for this disagreement is not known. In view of the generally good agreement for the other cases examined, the accuracy of the experimental results for this case is questioned.

One point which should be mentioned is the relationship between the percentage fluctuations in thrust and torque. Both the calculated and measured results show that the percentage fluctuation of thrust is greater than for torque. For the first harmonics, the measured percentage torque fluctuations fell in a range of between 45 to 65% of the measured percentage thrust fluctuations, compared to 65 to 75% for the calculated results. It is not clear why this discrepancy exists. Assuming the curves of open water thrust and torque coefficients to be approximately equidistant from each other over a range of advance coefficients (J'), the ratio of the amplitudes of percentage torque fluctuation to percentage thrust fluctuation according to the short method of calculation will be roughly the same as the ratio of the average thrust coefficient to ten times the average torque coefficient. It is of interest to note that in Reference 12, where a comparison is presented of the measured peak-to-peak thrust and torque fluctuations by two different experimental techniques, the Netherlands Ship Model Basin obtained a ratio of 47%, whereas the Hamburg Ship Model Basin obtained a ratio of 60% for the same ship and propeller. The agreement was good for thrust fluctuation, the discrepancy occurring for the measured torque fluctuation.

DISCUSSION AND CONCLUSIONS

On the basis of the generally good agreement between the thrust and torque fluctuations calculated by the short quasi-steady-state method and those measured, it appears that unsteady effects are secondary effects for the cases examined. This finding is especially surprising in view of the fact that in two-dimensional unsteady airfoil theory, unsteady effects are governing in the range of reduced frequencies for the propellers examined above. The first harmonic reduced frequencies ($\omega = \frac{\nu c}{2U}$) for propellers I, II, V, and VI fall within the range of approximately 1.2 to 2.0, where: ν is the first harmonic frequency of oscillation in radians per second ($\nu = 2\pi n Z$), c is the chord length at .7 radius, and U is the resultant inflow velocity at .7 radius. For this range of reduced frequencies, Sears[30] shows that for a two-dimensional airfoil operating in a sinusoidal gust, the unsteady lift fluctuations are less than one-half of those given by quasi-steady theory. This, in effect, appears to be what Breslin and Ritger[1] determined by applying Sears' two-dimensional unsteady theory to the calculation of fluctuating propeller thrust and torque. Contrary to this finding, Figures 11 and 12 indicate that in most cases, the quasi-steady predictions by the short method tend to slightly underestimate the peak to trough amplitudes of fluctuating thrust and torque. Further, since this study was begun, it has been rationally argued in the Authors' Closure to Reference 26, that correct unsteady theory for propellers could predict fluctuating forces which are higher or lower than those given by quasi-steady theory. It is evident, then, that more work must be done in order to find an adequate unsteady theory for propellers, which takes into account aspect-ratio effects and the distribution of shed vortices on helical sheets in a propeller's wake.

In regard to the Burrill method, or any other method of propeller design which is worked backwards to determine fluctuating propeller thrust and torque, further work must be done in order to find a design method which will predict the off-design performance of propellers. It is believed that the Burrill method, which was no doubt not developed for off-design predictions, has been used too hastily for the calculation of fluctuating thrust and torque and underestimates the slopes of the K_T and K_Q curves over a wide range of J values. Hence, the disagreement between the predictions of the short method and the Burrill method as applied in Reference 26, shown in Figure 8.

The short method outlined here, while it gives results which are in good agreement with experimental results, is intended primarily as a way of making rapid estimates of fluctuating thrust and torque, and was used here as a convenient tool for comparison with other methods and with experimental results. Before the short method can be applied with confidence, it will be necessary to compare its estimates with further experimental results for which complete information is available.

The advantage of the short method lies in its simplicity, and for this reason, it may be useful to naval architects during the preliminary stages of ship design. It treats total blade performance without considering separately the local performance at each radial section along a propeller blade. To summarize, the required data for making the calculation are:

1. Longitudinal and tangential wake description.

2. Propeller characteristic performance curves.

If propeller performance curves are unavailable, they may be estimated from published open water series data, as was done for some of the above examples.

REFERENCES

1. Breslin, J.P. and Ritger, P.D., "A Theory for the Quasi-Steady and Unsteady Thrust and Torque of a Propeller in a Ship Wake," ETT Report 686 (Jul 1958).

2. Reed, F.E. and Bradshaw, R.T., "Ship Hull Vibrations -2, The Distribution of Exciting Forces Generated by Propellers," CONESCO Report F-101-2 (Jun 1960).

3. Yeh, H.Y., "Thrust and Torque Fluctuations for APA 249, TMB Model 4414," David Taylor Model Basin Report 1364 (Jan 1960).

4. Brehme, H., "Der Einfluss der Flügelzahl eines Schiffspropellers auf die Erregung von Schiffsvibrationen," Schiff und Hafen, Vol 6, pp 662-666 (1954).

5. Schuster, S., "On the Hydrodynamically Determined Thrust and Torque Fluctuations in the Propelling Machinery of Ships," TMB Translation 275 (Mar 1959) (VDI - Zeitschrift, Vol 98, No. 32, 11 Nov 1956).

6. Schuster, S. and Walinski, E.A., "Beitrag zur Analyse des Propellerkraftfelders," Schiffstechnik HEFT 23, Band 4 (Sep 1957).

7. Tachmindji, A.J. and Dickerson, M.C., "The Measurement of Thrust Fluctuation and Free Space Oscillating Pressures for a Propeller," David Taylor Model Basin Report 1107 (Jan 1957).

8. van Manen, J.D. and Kamps, J., "The Effect of Shape of Afterbody on Propulsion," Trans. SNAME, Vol 67 (1959).

9. van Manen, J.D. and Wereldsma, R., "Propeller Excited Vibratory Forces in the Shaft of a Single Screw Tanker," International Shipbuilding Progress, Vol 7 (Sep 1960).

10. van Manen, J.D. and Wereldsma, R., "Dynamic Measurements on Propeller Models," International Shipbuilding Progress, Vol 6 (Nov 1959).

11. Mathews, S. and Benckhuysen, C., "Propeller Force Fluctuations; and Velocity Distributions in Ship Wakes and Slip Streams," Eastern Canadian Section of SNAME (Apr 12, 1960).

12. Krohn, J. and Wereldsma R., "Comparative Model Tests on Dynamic Propeller Forces," International Shipbuilding Progress, Vol 7, (Dec 1960).

13. van Manen, J.D. and Crowley, J.D., "Some Aspects of Circulation Theory Design of Screw Propellers," New York Metropolitan Section of SNAME (29 Oct 1959).

14. Burrill, L.C., "Calculation of Marine Propeller Performance Characteristics," Trans. NECI, Vol 60 (1943-44).

15. Løtveit, M., "A Study of Propeller Action in the Behind Conditions," European Shipbuilding No. 1 (1960).

16. Nichols, W.O., et al, "Some Aspects of Large Tanker Design," Trans. SNAME, Vol 63 (1955).

17. Hill, J.G., "The Design of Propellers," Trans. SNAME, Vol 57 (1949).

18. Eckhardt, M.K. and Morgan, W.B., "A Propeller Design Method," Trans. SNAME, Vol 63 (1955).

19. van Manen, J.D., "Fundamentals of Ship Resistance and Propulsion," Publication No. 132a of NSMB.

20. Kerwin, J.E., "Machine Computation of Marine Propeller Characteristics," International Shipbuilding Progress, Vol 6 (Aug 1959).

21. Lerbs, H.W., "Moderately Loaded Propellers with a Finite Number of Blades and an Arbitrary Distribution of Circulation," Trans. SNAME, Vol 60 (1952).

22. Troost, L., "Open Water Test Series with Modern Propeller Forms," Trans. NECI, Vol 67 (1950-51).

23. Bunyan, T.W., "Practical Approach to Some Vibration and Machinery Problems in Ships," Trans. INA, Vol 97 (1955).

24. Harvald, S.A., "Wake of Merchant Ships," The Danish Technical Press, Copenhagen (1950).

25. Horn, F., "Measurement of Wake," Trans. NECI, Vol 54 (1937-38).

26. Stuntz, G.R., et al, "Series 60 - The Effect of Variations in Afterbody Shape upon Resistance, Power, Wake Distribution and Propeller Excited Vibratory Forces," Trans. SNAME, Vol 68 (1960).

27. Saunders, H.E., "Hydrodynamics in Ship Design," SNAME, New York (1957) Vol II, Section 70.16.

28. Wylie, C.R., "Advanced Engineering Mathematics," McGraw-Hill, New York (1951), pp 542-53.

29. Peach, R.W., "Concerning the Relationship between Possible Excitation Modes in the Harmonic Components of an Input Force," Journal of American Society of Naval Engineers, Vol 73 (May 1961).

30. Sears, W.R., "Some Aspects of Non-Stationary Airfoil Theory and its Practical Application," Journal of Aeronautical Sciences, Vol 8 (1940-41).

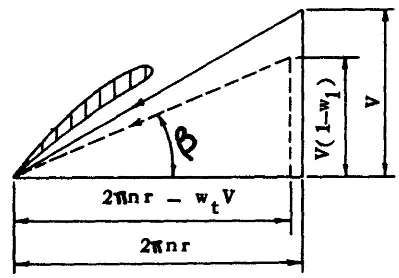


Figure 1 Local Inflow Velocity Diagram at a Propeller Blade Section

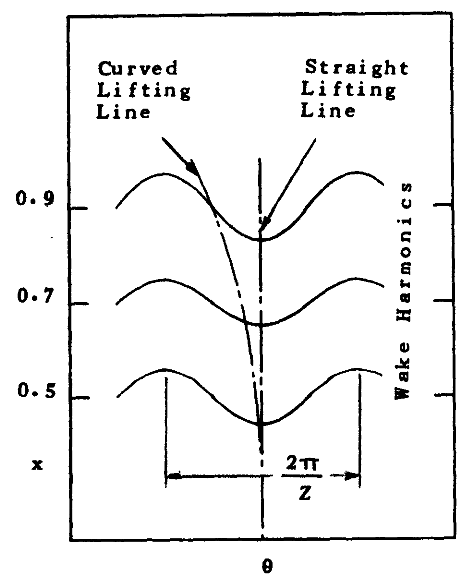


Figure 2 Sketch Showing the Effect of Skew on the Line of Encounter of a Blade with Assumed Wake Harmonics at Three Radii

Figure 3 Local Inflow Velocity Diagrams for Case (a) and Case (b)

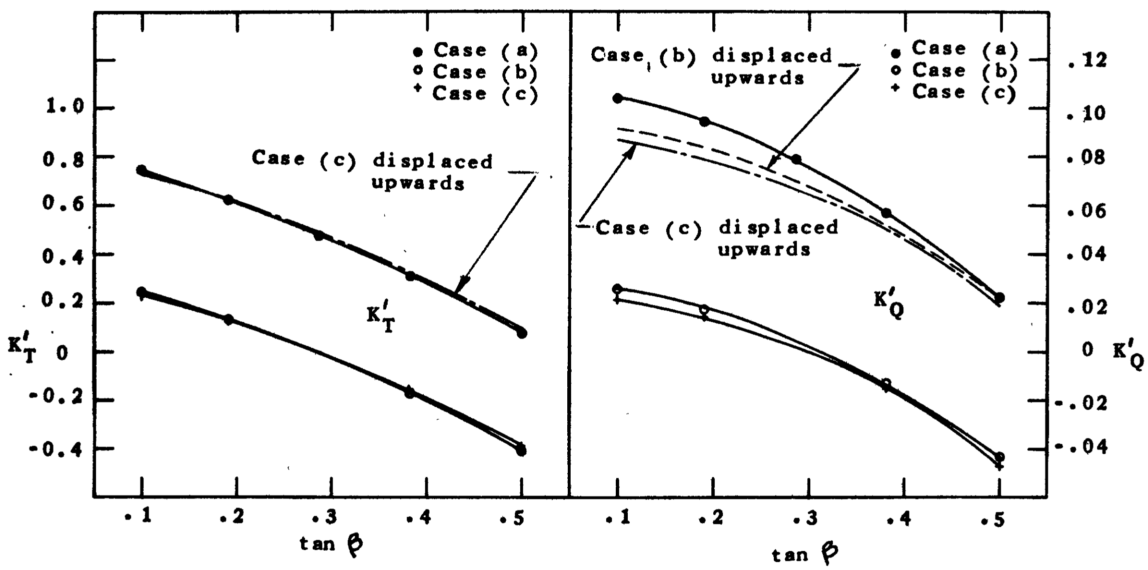


Figure 4 K'_T and K'_Q Curves Plotted Against tan β for Case (a), Case (b) and Case (c)

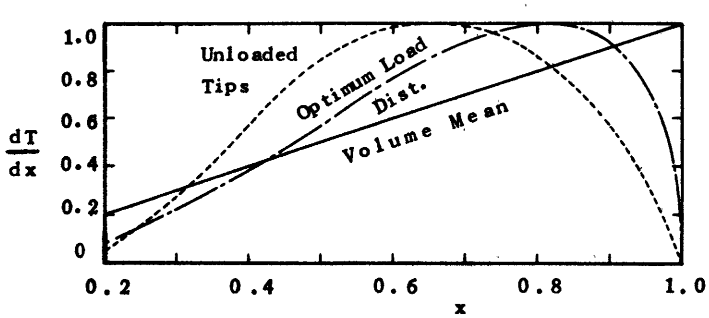


Figure 6 Weighting Curves Used in Calculating Local Weighted Mean Longitudinal Wakes

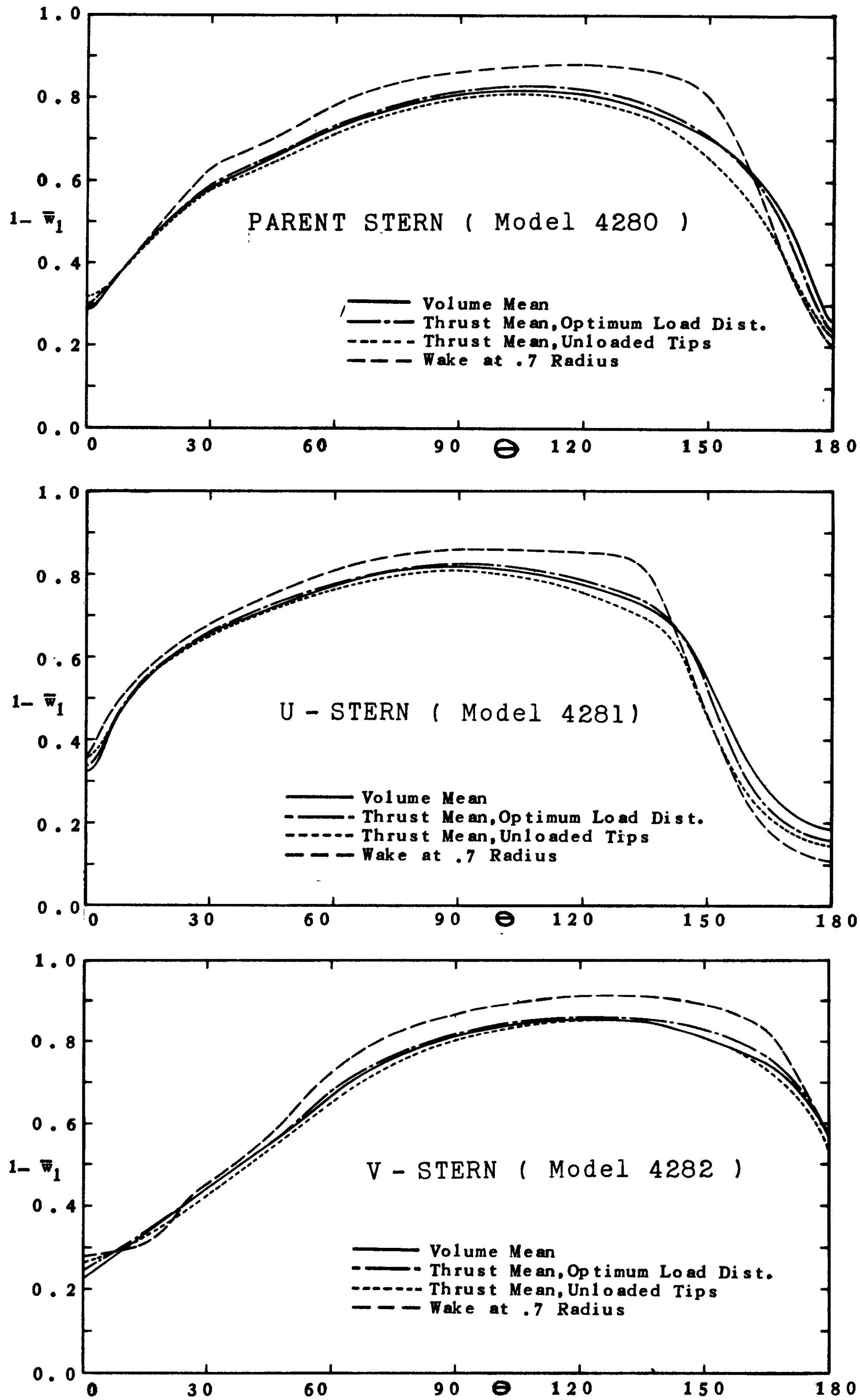


Figure 5 Local Weighted Mean Longitudinal Wakes from Blade Root to Tip, Plotted Against Angular Position, for Three Series 60 Sterns given in Reference 26

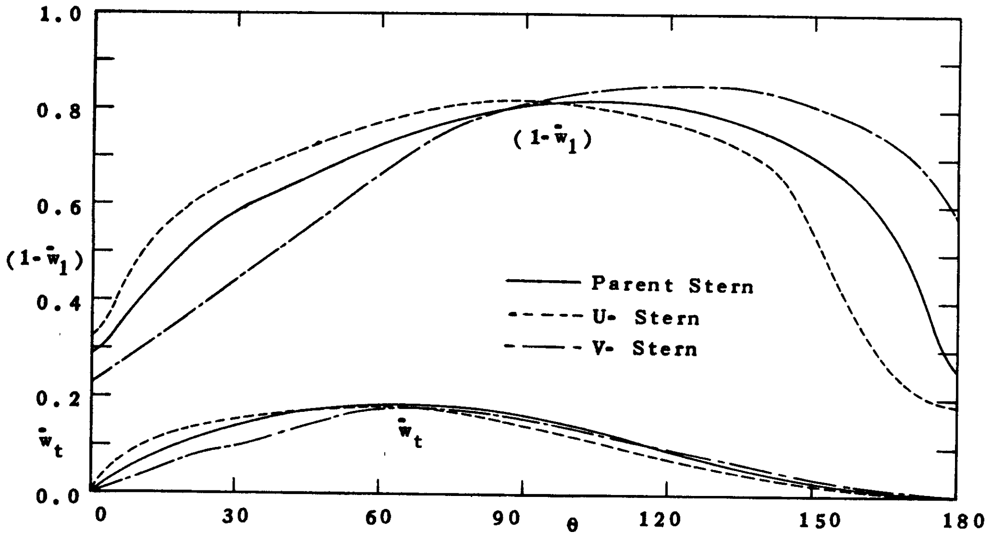


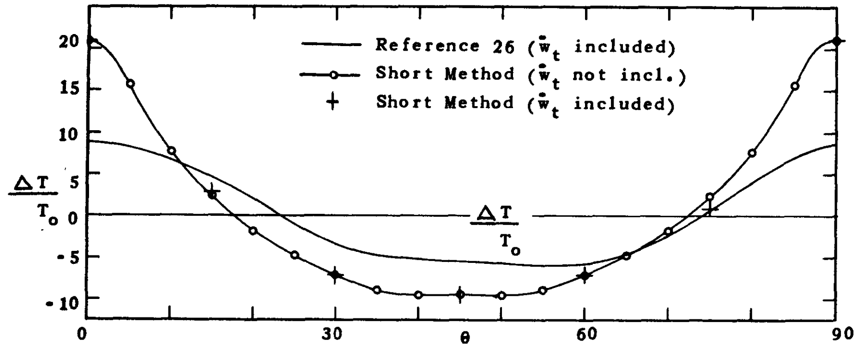
Figure 7 Local Volume Mean Longitudinal and Tangential Wakes for Series 60 Sterns given in Reference 26

TABLE I Mean Wakes for Series 60 Sterns

		PARENT	U-STERN	V-STERN
Calculated:	$(1-\bar{w}_1)_m$	.665	.642	.675
Model Test:	$(1-\bar{w}_1)_T$	.700	.660	.700
Ratio:	$\frac{(1-\bar{w}_1)_T}{(1-\bar{w}_1)_m}$	1.052	1.028	1.037

TABLE II Particulars for Propellers and Sterns of Reference 9

STERN	PROP	Z	$\frac{(1-\bar{w}_1)_T}{(1-\bar{w}_1)_m}$	d (ft)	V (knots)	N (rpm)	$J_T = \frac{V(1-\bar{w}_1)_T}{nd}$	$K_{T_o} = \frac{T_o}{\rho n^2 d^4}$	$K_{Q_o} = \frac{Q_o}{\rho n^2 d^5}$
Conventional	I	5	1.062	21.33	16.2	105.3	.453	.1822	.0239
Conventional	II	5	1.062	21.33	16.2	102.9	.464	.1932	.0257
Conventional	V	6	1.044	20.50	16.2	104.5	.467	.2130	.0297
Conventional	VI	4	1.062	21.33	16.2	104.2	.458	.1853	.0248
Mariner	II	5	1.072	21.33	16.2	101.6	.431	.2030	.0266
Mariner	VI	4	1.072	21.33	16.2	103.3	.425	.1954	.0254



PROPELLER 3376 (4 blades)

PARENT STERN (Model 4280)

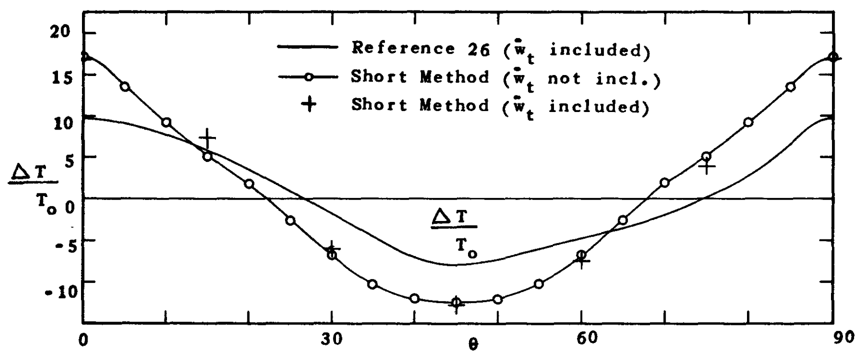
100% Displacement

Ship Speed - 17.5 knots

Calculated Harmonic Components as % of Average Thrust

THRUST

Harmonic	1^{st}	2^{nd}	3^{rd}
Reference 26 ($\dot{w}_t$ included)	7.2	1.7	0.2
Short Method ($\dot{w}_t$ not incl.)	12.6	4.1	1.9



PROPELLER 3376 (4 blades)

U-STERN (Model 4281)

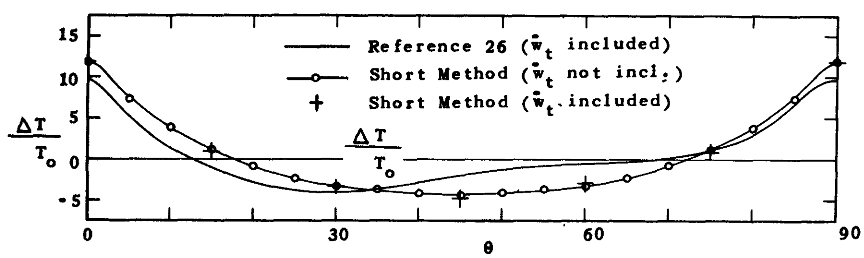
100% Displacement

Ship Speed - 17.5 knots

Calculated Harmonic Components as % of Average Thrust

THRUST

Harmonic	1^{st}	2^{nd}	3^{rd}
Reference 26 ($\dot{w}_t$ included)	8.9	0.7	1.4
Short Method ($\dot{w}_t$ not incl.)	13.5	1.1	1.0



PROPELLER 3376 (4 blades)

V-STERN (Model 4282)

100% Displacement

Ship Speed - 17.5 knots

Calculated Harmonic Components as % of Average Thrust

THRUST

Harmonic	1^{st}	2^{nd}	3^{rd}
Reference 26 ($\dot{w}_t$ included)	4.5	2.8	1.2
Short Method ($\dot{w}_t$ not incl.)	6.1	2.4	1.3

Figure 8 A Comparison of the Calculated Quasi-Steady-State Propeller Thrust Fluctuations given in Reference 26 with those Determined by the Short Method, for Three Series 60 Sterns

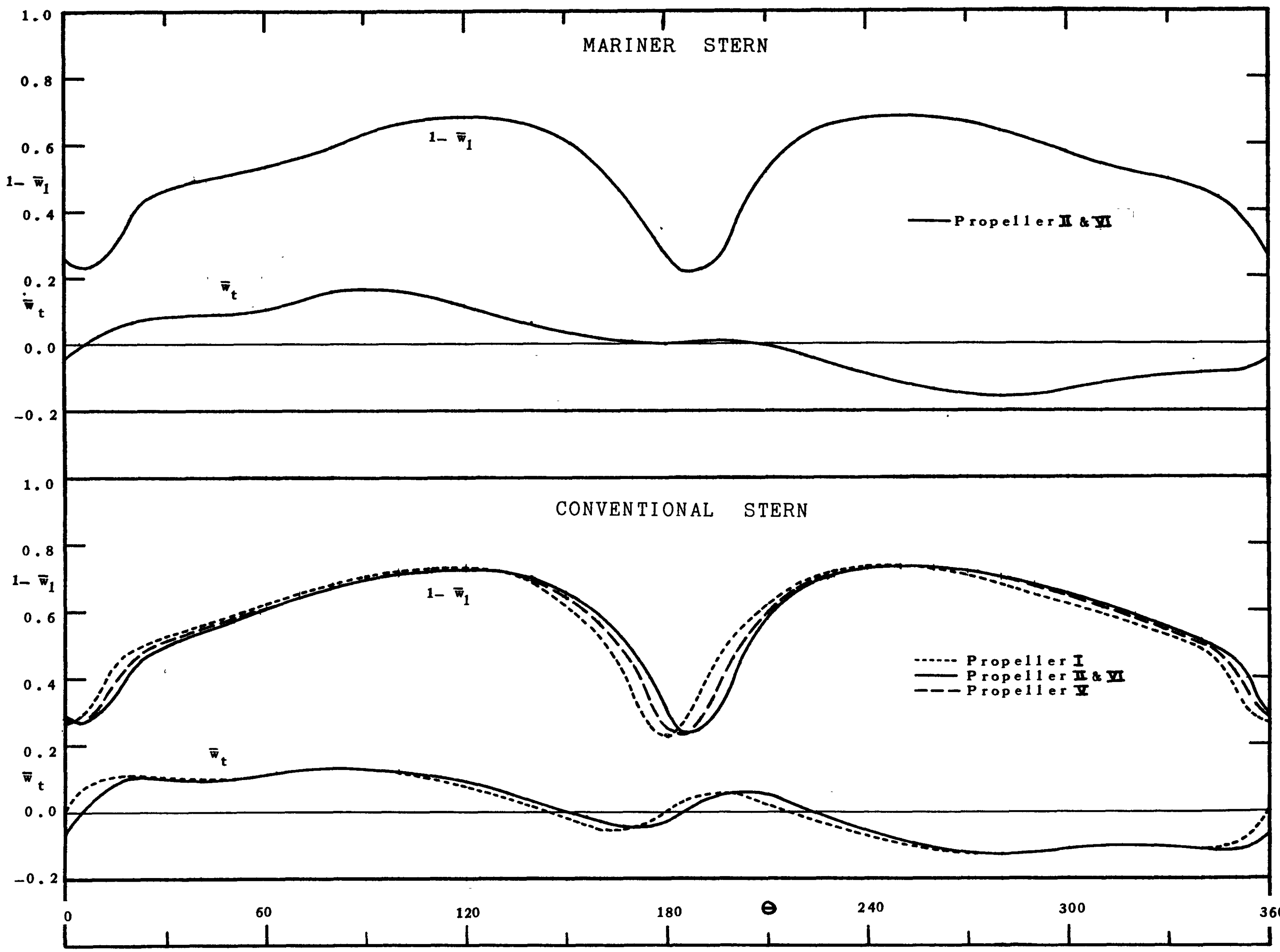


Figure 9 Local Volume Mean Longitudinal and Tangential Wakes for Several of the Propeller-Stern Combinations given in Reference 9

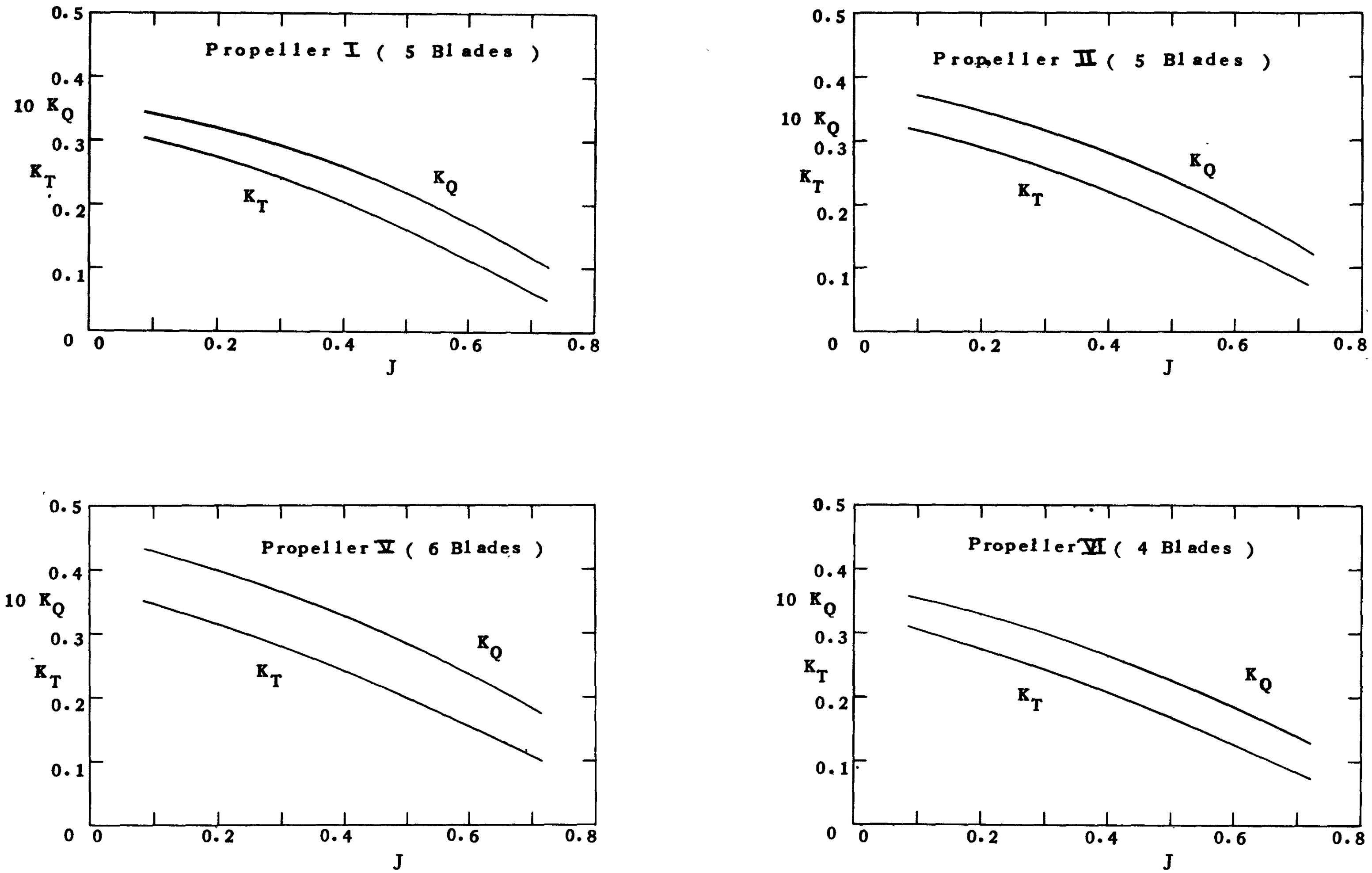
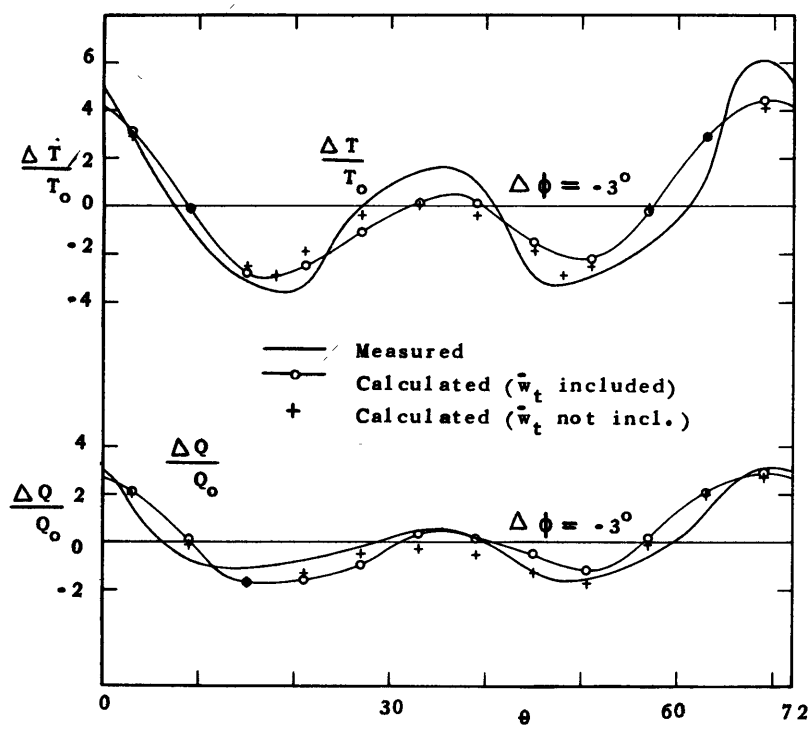


Figure 10 Estimated Open Water Performance Curves for Propellers I, II, V and VI



PROPELLER I (5 blades)

CONVENTIONAL STERN

Load Condition

100% Power Absorption

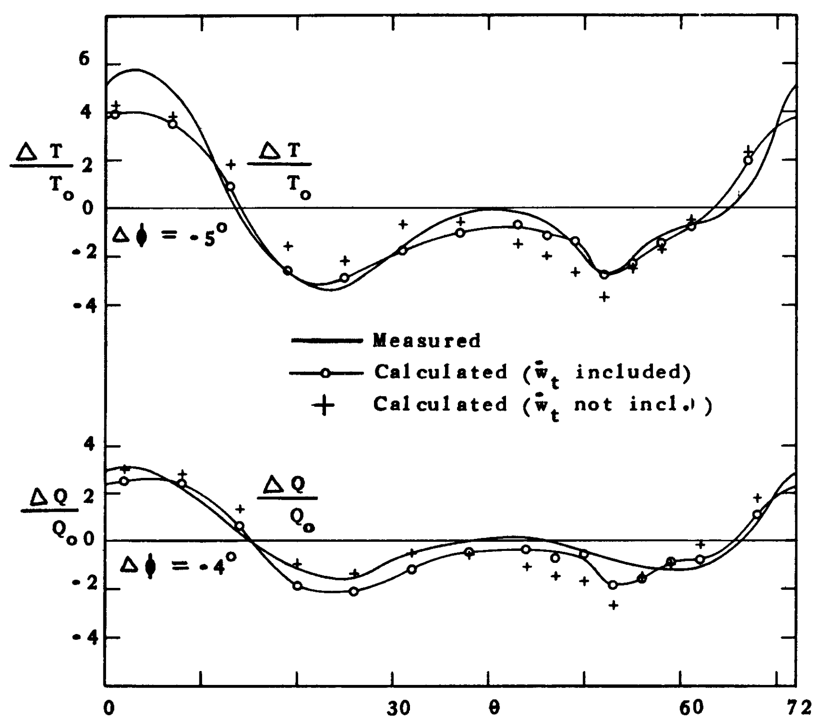
Harmonic Components as % of Average Values

THRUST

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	1.7	3.6	0.5
Calculated ($\bar{w}_t$ included)	2.1	2.5	0.1

TORQUE

Harmonic	1^{st}	2^{nd}	3rd
Measured	0.9	1.6	0.3
Calculated ($\bar{w}_t$ included)	1.4	1.5	0.1



PROPELLER II (5 blades)

CONVENTIONAL STERN

Load Condition

100% Power Absorption

Harmonic Components as % of Average Values

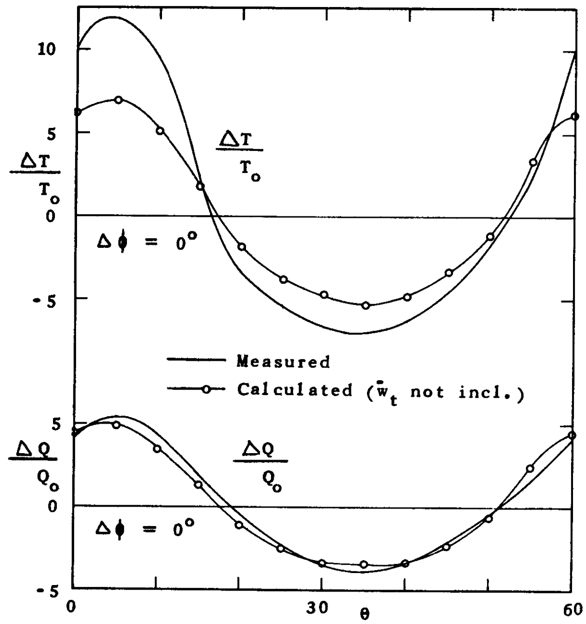
THRUST

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	2.4	2.8	0.5
Calculated ($\bar{w}_t$ included)	2.4	2.0	0.2

TORQUE

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	1.1	1.4	0.3
Calculated ($\bar{w}_t$ included)	1.5	1.5	0.1

Figure 11 A Comparison of the Measured Propeller Thrust and Torque Fluctuations given in Reference 9 with those Determined by the Short Method, for the Conventional Stern



PROPELLER V (6 blades)

CONVENTIONAL STERN

Load Condition

100% Power Absorption

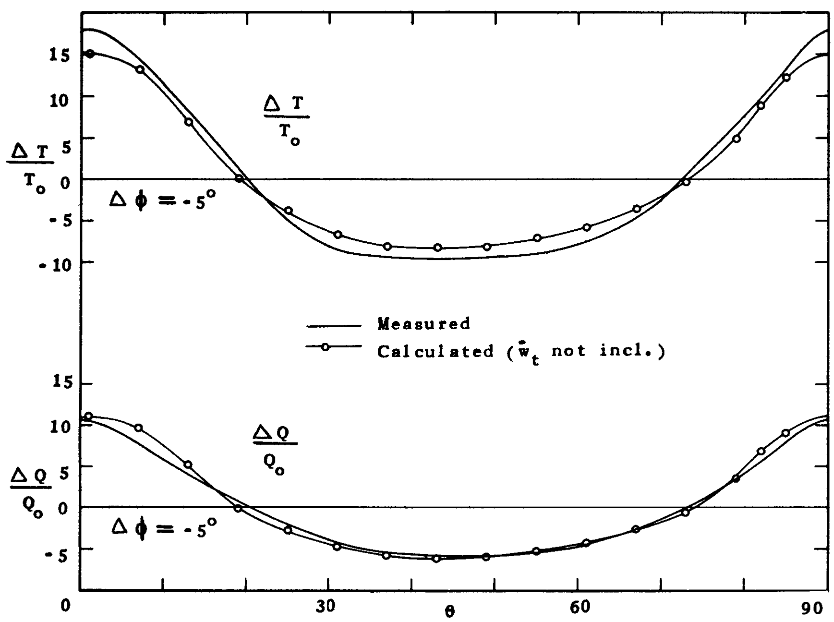
Harmonic Components as % of Average Values

THRUST

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	9.0	2.6	1.0
Calculated ($\bar{w}_t$ not incl.)	6.1	1.1	0.1

TORQUE

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	4.3	0.8	0.3
Calculated ($\bar{w}_t$ not incl.)	4.3	0.8	0.1



PROPELLER VI (4 blades)

CONVENTIONAL STERN

Load Condition

100% Power Absorption

Harmonic Components as % of Average Values

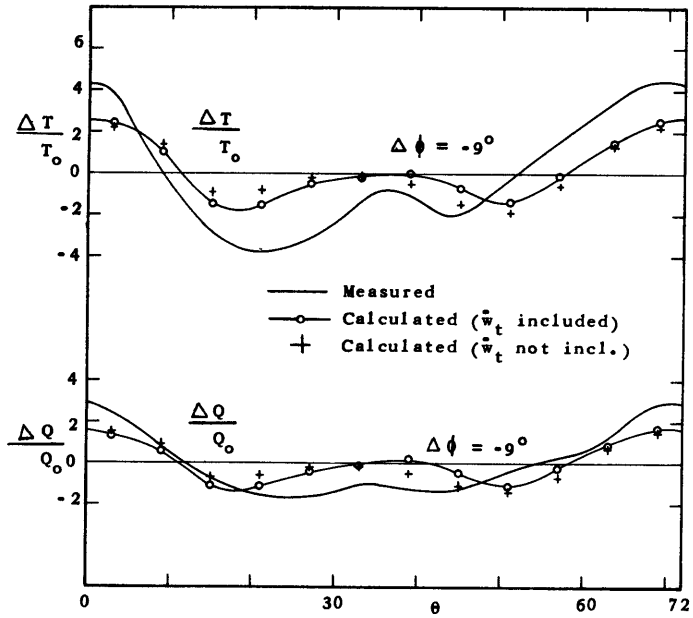
THRUST

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	13.0	3.8	0.6
Calculated ($\bar{w}_t$ not incl.)	10.8	3.4	1.0

TORQUE

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	7.5	2.1	0.7
Calculated ($\bar{w}_t$ not incl.)	8.0	2.6	0.8

Figure 11 (Continued)



PROPELLER II (5 blades)

MARINER STERN

Load Condition

100% Power Absorption

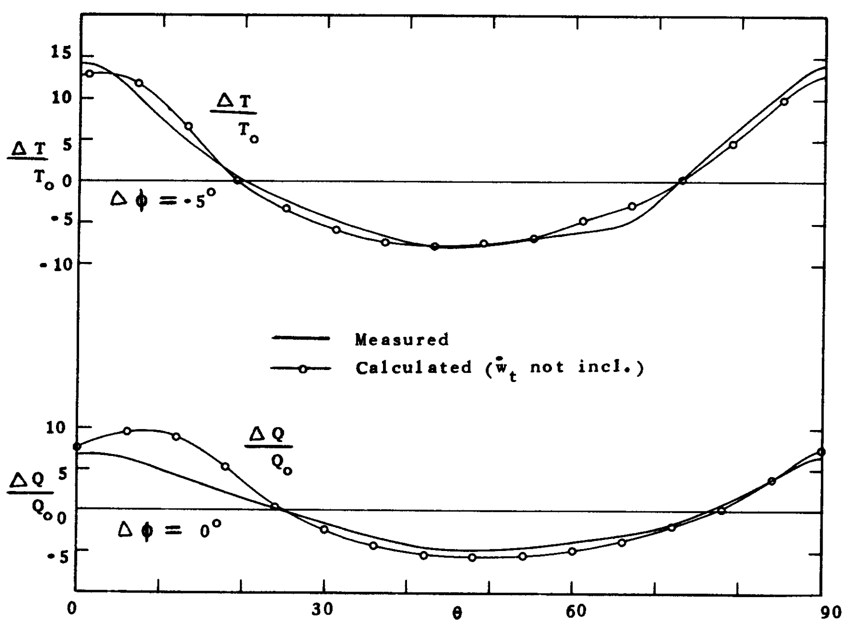
Harmonic Components as % of Average Values

THRUST

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	3.1	2.0	0.2
Calculated ($\bar{w}_t$ included)	1.2	1.3	0.5

TORQUE

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	2.0	0.7	0.1
Calculated ($\bar{w}_t$ included)	0.8	1.0	0.0



PROPELLER VI (4 blades)

MARINER STERN

Load Condition

100% Power Absorption

Harmonic Components as % of Average Values

THRUST

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	9.7	2.7	1.1
Calculated ($\bar{w}_t$ not incl.)	9.6	2.6	0.8

TORQUE

Harmonic	1^{st}	2^{nd}	3^{rd}
Measured	5.3	1.1	0.6
Calculated ($\bar{w}_t$ not incl.)	7.2	1.9	0.5

Figure 12 A Comparison of the Measured Propeller Thrust and Torque Fluctuations given in Reference 9 with those Determined by the Short Method, for the Mariner Stern

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