SESSION ON PROPULSOR

Chairman: Dr. K. Yokoo

Propulsor Committee Memberships: W. van Gent (Chairman) — T. Brockett (Secretary) — B. A. Biskoup — S. T. Dong — E. J. Glover — L. Grossi — C. S. Lee — F. B. Peterson

Discussion of the Report and the Draft Recommendations of the Propulsor Committee. (Cf. Proceedings, Volume 1, p.97 — 157.)

I. DISCUSSIONS

PR—1

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A CONTRIBUTION TO THE COMPARATIVE CALCULATIONS OF PROPELLER DESIGN

The present written discussion presents results from the design of the propeller, used for comparative calculations by the (members of the) Propulsor Committee, based on two approaches used at BSHC.
The input data for the calculations are taken from [1].

The aim of the investigation is to show the validity of these approaches, in the light of the problems raised by the Committee, related to the disagreement of the results when different calculation schemes are used.

Method 1 : On the lifting line stage the calculations are based on the classical approaches of Lerbs and Wrench in the particular case, the circulation is expressed by 10 terms of the classical sine type series.

On the lifting surface stage the calculations are based on the approaches of Pien and Cheng, additionally taking into account the effect of sections' thickness by sink / sources distribution.

On the lifting line stage the viscous roughness effects are taken into account on the basis of Hoerner and ITTC—57 formula and on the lifting surface stage corrections are introduced for the camber line and the ideal angle of attack. In the particular case, (as approved for comparison by the Committee), the viscous corrections are eliminated on both stages. The particularities of the method used are given in [5] and [6].

The basic results from the calculations are presented in Figs. 1 to 6. It is clearly seen that at the prescribed circulation distribution (Fig. 1) the (pitch and camber distributions) P/D, f/c = f(r/R) are in accordance with the results plotted in Fig. 13 (1) for Methods 1 and 2, which are statistical representatives of methods using constant radius vortex wake. The obtained value of the power loading coefficient $C_{pi}=1.262$ is also close to that of Method 2 ($C_{pi}=1.264$) and to the mean value of this quantity out of the 10 results presented ($C_{pi} = 1.257$).

The experience gained at BSHC in the past 8–9 years in the operation of the automated system for screw propeller design and manufacuturing, developed on the basis of the approaches described above and the repeated checking in experimental practice (in model and full–scale conditions) shows that the generated propeller geometry is underestimated with respect to pitch in the limits of 1–2% which is taken into consideration at the design stage.

Method II: also represents the application of the lifting line and lifting surface methods for the purposes of complex geometry screw propeller design. The algorithmization of the lifting line method follows V. Mishkevitch's approach [2]. Since the boundary condition for the circulation on the hub is met–there it has a finite value, the distribution indicated in [1] cannot be reproduced, and that is why an optimum propeller was designed (by Ivchenko–Stoyanov's criterion) and the results are presented in Figs. 1 to 6 as Method II.

The circulation distribution along the radius is shown in Fig. 1. Its radial derivative is zero at the hub and increases more rapidly towards infinity at the tip, as seen from Fig. 3. The respective distribution of $\tan\beta_I$ is shown in Fig. 4.

It should be pointed out that the presentation of *the circulation derivative in Jacobi polynomial* series [2] with a considerable number of terms, used on the lifting line level, ensures a very good presentation of the distribution both of the circulation and of its derivative. On the other hand, for calculation of the principal values of the integrals for the induction factors, determining the induced velocities, the quadrature formula of A. Korneichuk [2] is used with respective weighting function ensuring high accuracy of integration, proved by analytic validation. In this sense, this solution is free to a considerable extent from the shortcomings discussed in the Committee report.

The radial distribution of the pitch and camber shown in Figs. 5 and 6 are obtained by a lifting surface method. The lifting properties are presented by continuous vortex sheet distribution of bound and free vortices along a reference surface using orthogonal coordinates even for the cases of high pitch and rake gradients. The thickness is taken into account by sink/source distribution. The bound vorticity is presented in the form of double series (chordwise and radial modes) [3], and the induced velocities are calculated with the aid of vortex/source panels of first order [4]. The radial induced velocities are also included in the boundary conditions.

The differences between the distribution of the pitch (Fig. 5) and the maximum curvature (Fig. 6) are due mostly to the considerable difference in the circulation distribution along the radius.

A numerical experiment showed that in the case of optimum propeller both methods (I and II) give the same propeller efficiency equal to 0.603.

Finally we would like to outline the following conclusions:

1. It is definitely seen from the results presented by the Committee that regardless of the approaches and numerical procedures used, the divergences in the resultant *geometry are larger than admissible.*

2. It is evident that at present no approach can be preferred to the others, neither is this the aim of the present investigation, despite the fact it is clear to what extent the physics is taken into consideration.

3. Proceeding that in real practice the design of

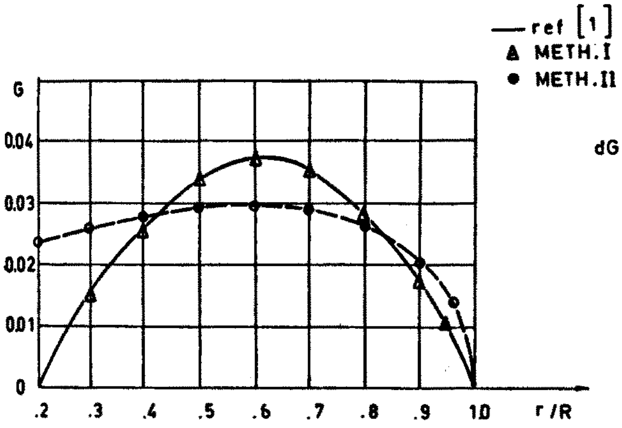

Fig. 1

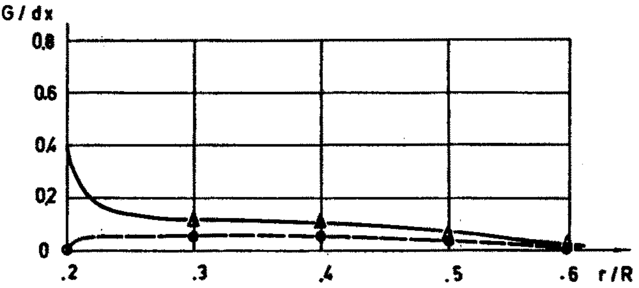

Fig. 2

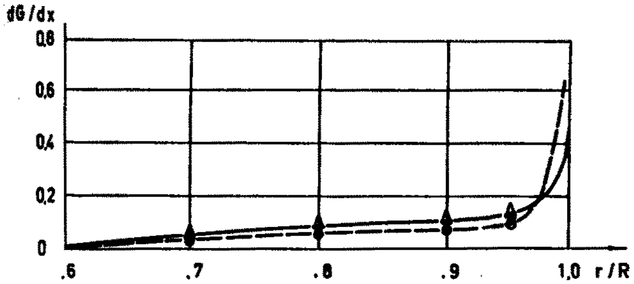

Fig. 3

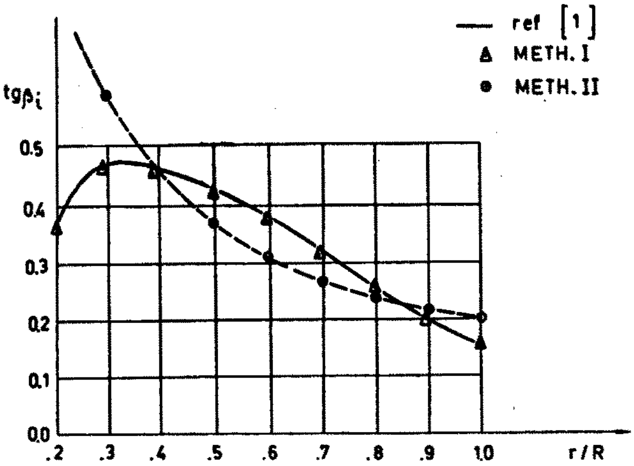

Fig. 4

Fig. 5

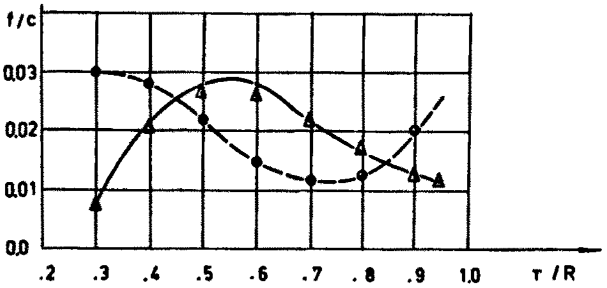

Fig. 6

a particular project is considerably more complex than this idealized case, when viscosity, roughness, optimality of loading distribution and reliable determination of the approaching flow for full scale conditions, it is logical to conclude that the divergences between the different approaches can become considerable.

4. The improvement of the accuracy of numerical solutions is a favourable factor for centring the attention on the improvement of the physical models. In this sense, we consider there is a vast field for the Validation Committee.

Finally, we would like to congratulate the Propulsor Committee for the effortes exerted in the systemizing the possible causes for scatter of the results, drawing the attention of the researchers towards the most important problems hoping that in its future work the joint efforts of the members will bring additional light on the problem.

References

[1] ITTC' 87 Report of Propulsor Committee

[2] Bavin V. F., N. Y. Zavadovsky, Y. L. Levkovsky, V. G. Mishkevitch,:"Propellers Modern Methods of Calculation", Izd. Sudostroeniye, Leningrad, 1983 (in Russian)

[3] Zavadovsky N. Y.,: "Theoretical Principles for Calculation of Hydrodynamic Characteristics of Propellers of Complex Geometry", Voprosy Sudostroeniya, vol.39, 1984 (in Russian)

[4] Varsamov K., T. Videv, A. Haimov, N. Y. Zavadovsky;: "Numerical Design and Analysis of Propellers with Complex Blade Shape", SMSSH' 86 Vol. 3, BSHC, Varna, 1986

[5] Hadjimikhalev V.,: "Automated System for Subcavitating Propellers Design, Vth Symp. of Ship Hydrodynamics", Gdansk, 1979 (in Russisn)

[6] Hadjimikhalev V., K. YOSSIFOV,: "Some Major Problems and Results Connected with Automated Propeller Design", 2nd IMAEM Congress, Trieste, 1981

PR−2

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COMMENT TO THE REPORT OF THE ITTC−PROPULSOR COMMITTEE OF THE 18TH ITTC

The report of the propulsor committee gives an interesting comparative study on the calculation of pressure fluctuations. One of the sensitive elements in such a calculation is the wake input and the committee advocates the use of an effective wake distribution (Fig. 1).

In the comparative calculations four participants used the total velocity distribution to calculate the effective wake. According to eq. 1 the use of the total velocity distribution is straightforward, since the effective wake is the wake distribution which has to be used in the potential propeller calculation to arrive at the total velocity field. Although no distinction is made in Fig. 2, there seems to be considerable scatter between these four results too. This means that there is scatter in the calculated induced velocities. This can be due to the calculated propeller loading or due to the calculated induced velocities at a certain propeller loading or both.

The question is if there is still such a large scatter in the effective wake for these four cases when the thrust is fixed?

In the case where the nominal wake was used to calculate the effective wake, were the total velocities also calculated and compared with the measured values?

These questions can put in a wider scope. To arrive from the nominal to the effective wake distribution two iterative calculations are needed : a calculation of the rotational propeller wake interaction and a calculation of the propeller hull interaction. There are no established procedures for either calculation. Since the propeller calculations are very sensitive to the wake it seems advantageous to use the total velocity distribution instead of the scheme given by the propeller comittee. Can the propulsor commmittee give an opinion on this matter?

The committee "does not encourage the use of lifting surface correction factors for cases different from those on which the factors are based." It is our experience that one can be more specific. The use of one lifting surface correction factor for a blade section is reasonably good for symmetrical profiles. Kerwin and Lee (1978) had this experience also. Deviations are found in the tip region (r/R>0.9), where the blade loading is no longer symmetrical. The use of one lifting surface correction leads to erroneous results in the case of a non-symmetrical blade loading or camberline. This is importatnt because the use of new blade sections also frequently implies a non-symmetrical camber, e.g. when "Eppler-profiles" are used.

The blade loading in a wake varies with the blade position. These variations can be separated into variations of the angle of attack and variations of the loading distribution or the "effective camberline". We found that these variations remain small relative to the effects of angle of attack variations.

So once the "effective camber" or the blade loading distribution is found in the steady case, the variations of the blade loading in a wake can be properly discribed by angle of attack variations.

PR-3

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ON UNSTEADY PRESSURE DISTRIBUTION ON PROPELLER BLADES IN NON-UNIFORM FLOW

Comparative calculation of the pressure fluctuations induced by a propeller has been carried out by the Propulsor Committee, 18th ITTC. It was concluded that the predicted pressure fluctuations on a flat plate induced by a non-cavitating propeller operating in uniform flow showed generally acceptable correlation among different calculation methods and with experimental data but the correlation of the predicted pressure induced by non-cavitating and cavitating propellers operating in non-uniform flow was not always good. In order to find the reason for the discrepancies, between the experiments and the calculations, unsteady pressure distributions on a propeller blade must be investigated at first.

There are very few experimental data of the unsteady pressure distributions on a propeller blade in non-uniform flow [1] because of the difficulties in measuring the pressures with high frequency response by using small transducers. Further, the unsteady pressure distributions on a propeller blade are usually calculated based on the assumption of an equivalent two-dimensional blade section, the lift of which is assumed to be same as the one obtained by numerical lifting surface theory.

A numerical method for solving the propeller lifting surface problems has been developed in the Nagasaki Experimental Tank MHI [2,3]. This method is based on a Quasi—Continuous Method (QCM) development by Lan [4] to improve the conventional vortex lattice method in such ways as wing edge and Cauchy singularities are properly accounted for. In the QCM, continuous loading distribution in chordwise direction is assumed, and therefore the pressure distributions on a propeller blade in both uniform and non—uniform flows can be calculated directly without any assumption.

Measurements of the unsteady pressure distributions on propeller blades were recently conducted in the cavitation tunnel of the Nagasaki Experimental Tank to confirm the accuracy of the QCM calculation of the blade surface pressures. In order to measure the blade surface pressure with high frequency response and high accuracy, small pressure gauges (6mmϕ × 0.6mm^{t}) were mounted in the small chamber inside the blade in the same way as used in References [5]. Pressure hole and the chamber were made vacuum and filled with silicon oil. Pressure signals were transmitted through a slip ring with 32 channels to amplifiers and data were sampled and analyzed by a mini—computer.

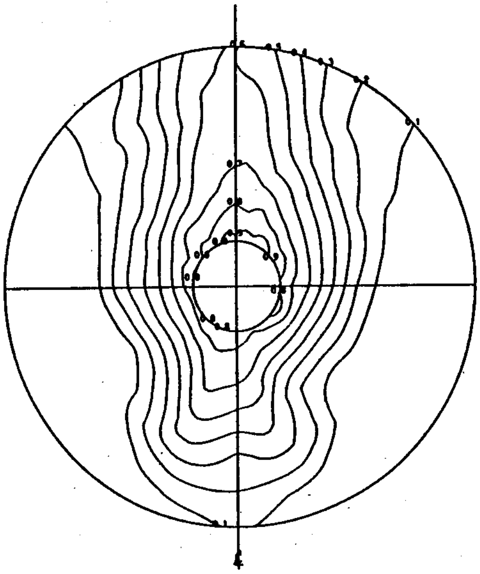
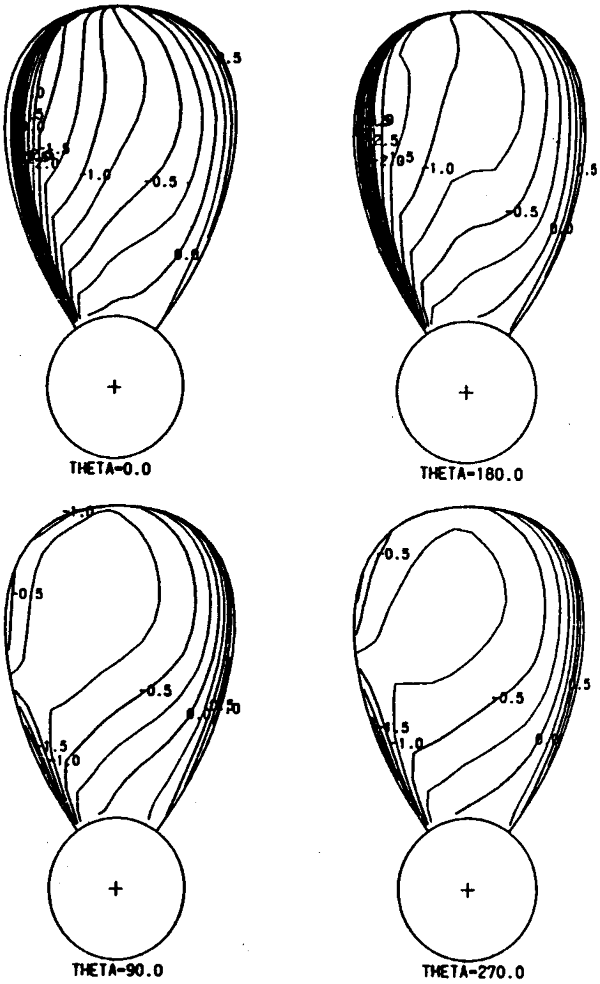


Fig. 1 Wake Contour Curves Simulated in Cavitation Tunnel



THETA=0.0 THETA=180.0

THETA=90.0 THETA=270.0

Fig. 2 Calculated Pressure Contour Curves on Back Side of Propeller Blade (C_p)

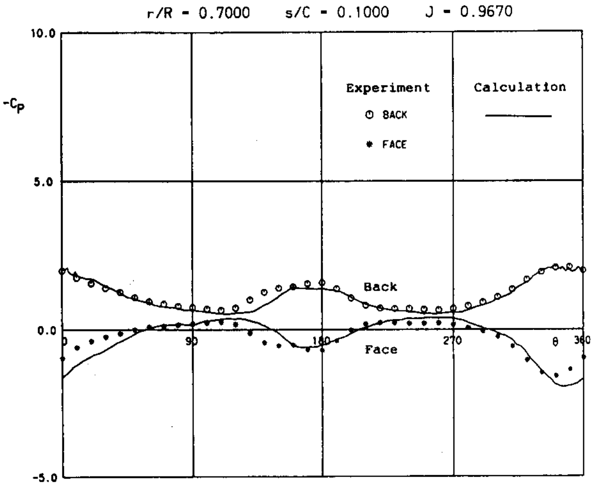


Fig. 3 Comparison of Fluctuating Pressure on Propeller Blade

Flow field simulated in the cavitation tunnel by wire mesh screen is shown in Fig. 1. Pressure contour curves on the back side of the propeller blade calculated by the QCM are shown in Fig. 2. Pressure fluctuations measured near the leading edge of s/C=0.1r/R=0.7 are shown in Fig. 3, comparing with the QCM calculations. Agreement between the experiments and the calculations is fairly good including peak values and phases.

Prediction of the unsteady pressure distributions on the blades of the propeller operating in non−uniform flow would be the base to estimate the unsteady cavitation patterns of the propeller. Further experimental and theoretical studies to estimate the unsteady pressure distributions on the propeller blade are going on in the Nagasaki Experimental Tank.

References

[1] Takei, Y., Koyama, K. and Kurobe, Y. : "Measurement of Pressures on a Blade of a Propeller Model", Papers of Ship Research Institute, No.55, 1979.

[2] Nakamura, N. : "Estimation of Propeller Open−Water Characteristics Based on Quasi−Continuous Method", Journal of the Society of Naval Architects of Japan, Vol. 157, 1985.

[3] Hoshino, T. : "Application of Quasi−continuous Method to Unsteady Propeller Lifting−Surface Problems", Journal of the Society of Naval Architects of Japan, No. 158, 1985.

[4] Lan, C. E. : "A Quasi−Vortex−Lattice Method in Thin Wing Theory", Journal of Aircraft vol.11, No. 9, 1974.

[5] Koyama, K., Kakugawa, A. and Okamoto, M : "Experimental Investigation of Flow Around a Marine Propeller and Application of Panel Method to the Propeller Theory", 16th Symposium on Naval Hydrodynamics, Berkeley, 1986.

PR−4

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COMMENT TO THE REPORT OF THE 18TH ITTC PROPULSOR COMMITTEE

Under the auspices of the 18th ITTC Propulsor and Cavitation Committes, two roundtable discussions were held in London Heathrow on the subject of "Design, Application, and Test of Propellers with New Blade Sections". The first meeting was held on 15 & 16 November 1985 and attended by 8 representatives from

- Centro Esperienze Idrodinamiche
- SSPA
- HSVA
- University of Tokyo
- MARIN
- DTNSRDC

The second meeting was held on 19 & 20 June 1987 and attended by 8 representatives from

- Centro Esperienze Idrodinamiche
- CENTENA
- DTNSRDC
- MARIN
- Escher Wyss
- ARE (Hasler)
- University of Tokyo

The intent of these meetings was to assemble experts on the various aspects of this immerging new technology. The meetings were informal and limited in attendance to those who have been actively working in the subject area. The agenda of the meetings was to share progress and ideas for future direction of this new technology and also to discuss the key issues that should be considered in the Propulsor Committee Questionnaire on Scale Effects. The attendance and discussion at the meetings confirmed that this is an active area of interest for propulsor designers and those concerened with propulsor evaluation.

The predominant analytical method used to design new blade sections is that developed by Eppler and Shen. However, encouraging results have also been achieved by empirical methods. Experience with model scale propellers has shown that new analytically designed two dimensional blade sections must be modified to reflect three dimensional camber effects and the blades may need to be repitched relative to the use of conventional sections. The most common propeller design method that has evolved is to use the two dimensional load distribution and a three dimensional camber distribution in a lifting surface computer code. Iterations are then required to match the pressure distritubions of the two dimensional sections. when variable thickness and camber distributions are used then special care is required in the fairing of the blade sections. Overall, the general trend is to design the sections with relatively thick leading edges.

To date, it appears that the primary motivation in the design of propellers with new sections is to suppress either leading edge cavitation or cloud cavitation, or to stabilize the blade cavity. The result is typically that the leading edge suction peak is reduced and scale effects become more significant.

One subject that resulted in considerable discussion was the extensive laminar boundary layer that can occur at model scale. This laminar boundary layer can suppress leading edge cavitation and thus enhance the scale effects. Thus the question of how to stimulate inception that occurs with the turbulent boundary layer on the full scale propeller was discussed at length. Several participants recommended use of artificial roughness, mainly at the leading edge, to reduce the scale effect but the method must still be verified by comparison with full scale propeller performance. Sand grains in epoxy were the most commonly used roughness but paint and sand blasting are also being used. Some participants are selecting the sand grain size on the basis of a roughness Reynolds number with a value of 750 suggested. Certainly from the discussion, it was concluded that the manufacturing accuracy of the leading edge is very important.

Some full scal results of propellers with empirically developed blade sections is available and indicated improved cavitation performance with lower radiated noise. A bulk carrier propeller with sections based on the Eppler code has had a full scale trial but details were not yet available.

The opinions, progress and plans that were shared by the participants in this intensive but informal setting were considered very valuable. Since activites in this subject area appear to be accelerating, it is recommended that it should continue to receive attention by both the Propulsor and Cavitation Committees of the 19th ITTC.

PR—5

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A METHOD OF DESCRIBING THE GEOMETRIC SHAPE OF FREE VORTEX WAKE

Two numerical lifting surface methods have been developed and widely used in hydordynamics, i.e. the mode function method and the discrete vortex method. However, the common shortcoming of the two approaches is that the nonlinear effects of the wake shape are ignored and the geometric shape of the free vortex wake has to be presumed.

Recently an unsteady nonlinear vortex lattice method (UNVLM) has been applied to calculating the hydrodynamic forces acting on the rudder and on the rudder in combination with additional thrusting fins (A. T. Fins) behind ship propeller, wherein the flow separates at tip edges [1,2]. We assume that the UNVLM is based on potential flow theory and the viscous effects can be represented by resonable flow modelling Both tip edges and trailing edge wake rollup are determined numerically by a time — dependent vortex shedding procedure together with the hydrodynamic characteristics.

Some numerical exapmles are performed to illustrate the efficiency and the accuracy of the method Calculations are carried out for the hydrodynamic forces of three rectangular rudders with NACA profile and thickness ratio $t_m/c=0.18$. The aspect ratio $\lambda=2.4$, 1.0, 0.2 respectively. The hydrodynamic researches of the rudder + A. T. Fins combination pertaining to a 12700T merchant ship were made. A theoretical design method for A. T. Fin has been developed.

The comparison between calculated and experimental results for rudders in uniform flow field is shown in Fig. 1.

The variation of the geometric shape of the free vortex sheet at time steps $t=t_0+4\Delta t$, $t=t_0+7\Delta t$, $t=t_0+9\Delta t$ and $t=t_0+20\Delta t$ for a rudder of unit aspect ratio and the angle of attack $\alpha=20°$ is shown in Fig. 2. After $t=t_0+20\Delta t$ the further variation of geometric shape of the free vortex sheet makes no influence on the hydrodynamic characteristics of the rudder.

Fig. 3 shows the calculated results of ΔK_{TP} of the rudder+A. T. Fins combination pertaining to the 12700T merchant ship at different angle of incidence β_F of A. T. Fin for H=O, where ΔK_{TP} stands for the increment ratio of propulsive efficiency (if the propulsive factors keep constant) and H is the height of A. T. Fin

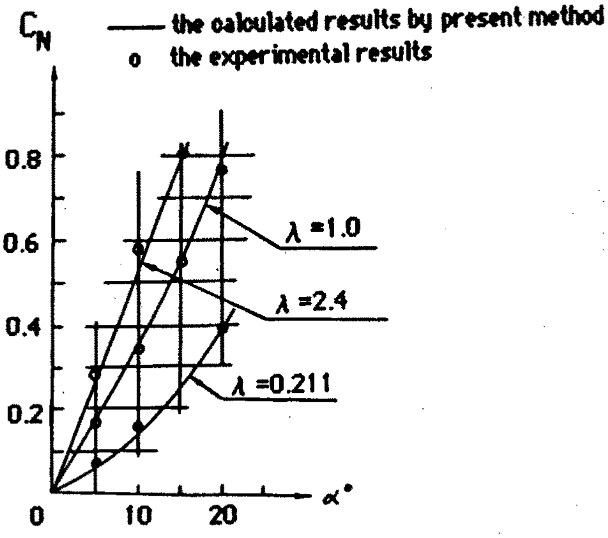
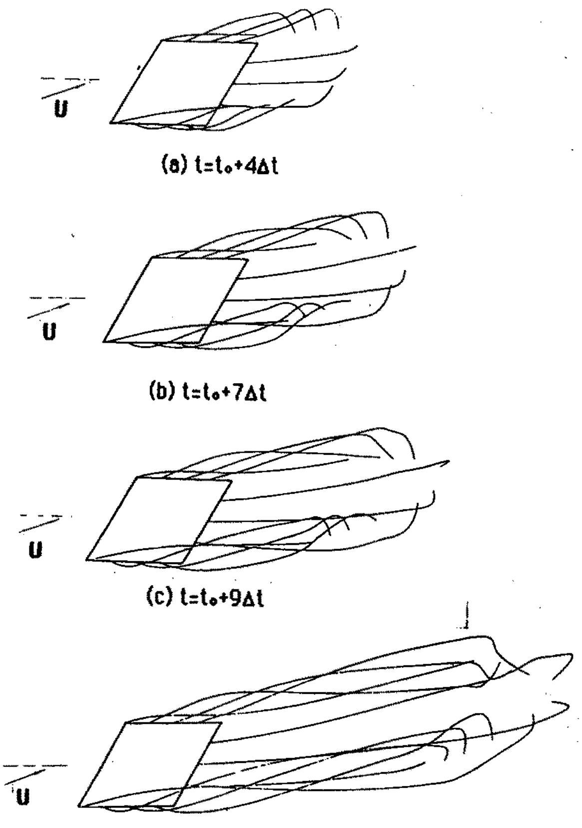


Fig. 1 Normal Force Coefficient Cn vs. Angle of Attack (Comparison Calculated and Experimental Results)



(a) $t=t_0+4\Delta t$

(b) $t=t_0+7\Delta t$

(c) $t=t_0+9\Delta t$

(d) $t=t_0+20\Delta t$

Fig. 2 The Development of Free Vortex Sheet at Different Time Steps ($\lambda=1.0$, $\alpha=20°$)

above the shaft center of the propeller. In Fig. 3 the experimental data are also given.

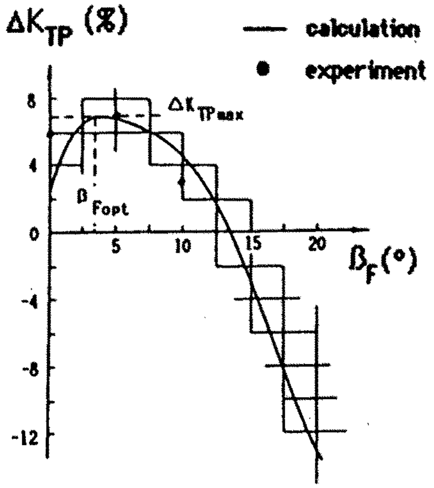


Fig. 3 Variation of Increment Ratio of Thrust $\triangle K_{TP}$ with Angle of Incidence B_F

The close agreement of the calculated and experimental results shows that the UNVLM is a valuable tool for predicting the hydrodynamic characteristics of rudder in uniform flow field, rudder and rudder + A. T. Fins combination in the propeller slipstream behind a ship and that it may be a promising beginning for accurately describing the geometric shape of propeller free vortex wake sheet.

References

[1] Wang Guoqiang and Zhang Tianfeng, :"A Nonlinear Vortex Lattice Method for Calculating Hydrodynamic Forces of Rudder with Tip Edge Separation", the IMAEM '87 iv congress. Varna, Bulgaria, 1987, also Journal of shipbuilding of china, No. 97, 1987

[2] Wang Guoqiang and Zhang Tianfeng, :"Design of Additional Thrusting Fin with Nonlinear Vortex Lattice Method", PRADS '87, Trondheim, Norway, 1987

PR−6

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APPLICATION OF A NEW TRAILING VORTEX WAKE MODEL TO PROPELLERS

The lifting−surface theory has been widely applied to the prediction of propeller performances. However, the accuracy of computation is affected for heavily loaded propellers or propellers under off−design conditions due to the nonlinear effects such as trailing vortex sheet deformation and slipstream contraction, etc. Therefore an appropriate trailing vortex wake model is very important for the numerical analysis of propeller performances.

A new trailing vortex wake model is proposed in the SHL [1], taking into consideration the nonlinear effects such as slipstream contraction. The trailing vortex wake is divided into two parts, namely, the transition wake region and the ultimate wake region. A conical helicoid is used to describe the deformed trailing vortex sheet in the transition wake region, and the ultimate wake region is simplified as K concentrated helical tip vortices together with a single hub vortex.
Emanating from the trailing edge at radius r, the trailing vortex with pitch angle β_T (r) is assumed to take the shape of a conical helix, which contracts to a prescribed radius at the end of the transition wake region. Therefore the trailing vortices form a conical helicoid.

The coordinates of a trailing vortex in the shape of a conical helix can be written as

$$\begin{cases} y' = Ce^{\alpha\theta'}\cos\theta' \\ z' = Ce^{\alpha\theta'}\sin\theta' \\ dx'/[r_T(\theta')\, d\theta'] = tg\beta_T \end{cases} \qquad (1)$$

$$r_T = \sqrt{y'^2 + z'^2} = Ce^{\theta'} \qquad (2)$$

Where C and α are constants to be determined, β_T, the pitch angle, is assumed to be only dependent on the radius, $r_T\ (\theta')$ is the varied radius of trailing vortex in the transition wake region. The new wake model is illustrated in Fig. 1.

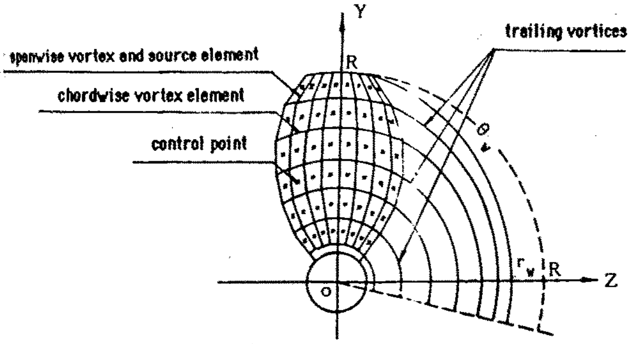

Fig. 1 Singularity Arrangement and Wake Model

To determine the geometry of the trailing vortex wake, certain parameters must be provided at the outset, and they are chosen as follows.

r_w ⋯ radius of the concentrated tip vortices;
θ_w ⋯ angular range of the transition wake region;
$\beta_T\ (r)$ ⋯ pitch angle of the trailing vortices in the transition wake region;
β_w ⋯ pitch angle of the tip vortices in the ultimate wake region.

Following is a concise description of how the parameters are determined.

By utilizing some of the experimental results given by Nagamatsu and Shimizu [2], a set of empirical formulae are obtained as follows:

$$\begin{cases} r_w\ (r)\ /r = 1.0\ ;\ (r/R < 0.7) \\ r_w\ (r)\ /r = 1.0 + (0.00125\theta m\ (R)\ - 0.17) \\ \quad (3.3333r/R - 2.3333);\ (r/R \geqq 0.7) \end{cases} \qquad (3)$$

Where θm (R) is the skew angle in degree, R is the radius of the propeller.

It is assumed for other three parameters that

$$\theta_w = 90^\circ \qquad (4)$$

$$\beta_T\ (r)\ = 0.4[\beta_0\ (r)\ - O\ (r)\ ] \qquad (5)$$

where $\beta_0\ (r)\ = \text{arctg}\ (V_A/2\pi\eta r)$, $O\ (r)\ = \text{arctg}[P\ (r)\ /2\pi r]$, and P (r) is the pitch of blade section.

$$\beta_w = \text{arctg}\ (P_{tip}/2\pi R) \qquad (6)$$

where P_{tip} is the pitch of propeller tip.

As soon as the above parameters are chosen, the constants C and α in (1) can be determined by using the conical helicoidal trailing vortex wake model and the fourmulae determining the geometry of the trailing vortices in the transition wake region read as follows:

$$x' = r \cdot \text{tg}\beta_T\ (e^{\alpha\theta'} - 1)\ /\alpha$$

$$\begin{cases} y' = re^{\alpha\theta'}\cos\theta' \\ z' = re^{\alpha\theta'}\sin\theta' \\ r_T = re^{\alpha\theta'} \end{cases} \qquad (7)$$

where

$$\alpha = \ln\ (r_w/r)\ /\theta_w \qquad (8)$$

Calculations are carried out for different propeller types by using the new trailing vortex wake model and the corresponding numerical method [1].

The caluclations include circulation distribution over radius and chords, open water characteristics, pressure distribution and the induced velocity field behind the propeller. Some of the numerical results are shown in Figs. 2−9 together with experimental results and those obtained by other prediction methods [3.4].

Investigations on propeller design have also been perfomed, in which Kerwin's wake model [3] and the new wake model are employed respectively [5].

Two of DTNSRDC series of research propellers

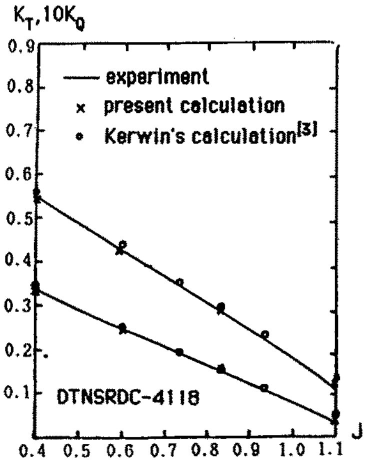

Fig. 2 Comparison of Open—Water Characteristics for DTNSRDC Propeller 4118

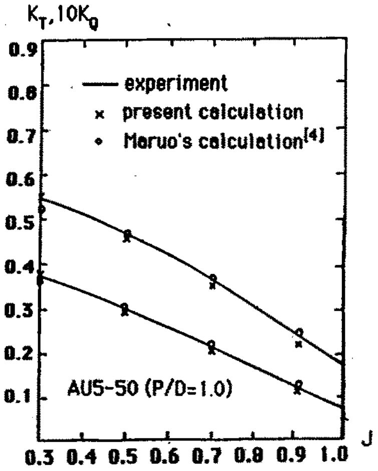

Fig. 3 Comparison of Open—Water Characteristics for Propeller AU5—50

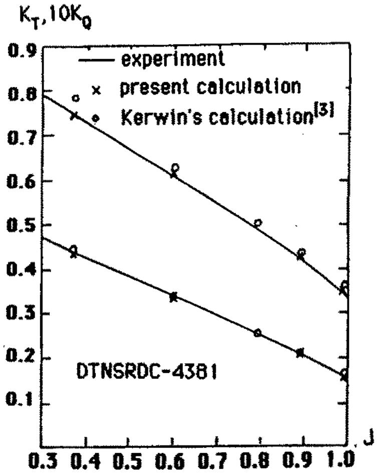

Fig. 4 Comparison of Open—Water Characteristics for DTNSRDC Propeller 4381

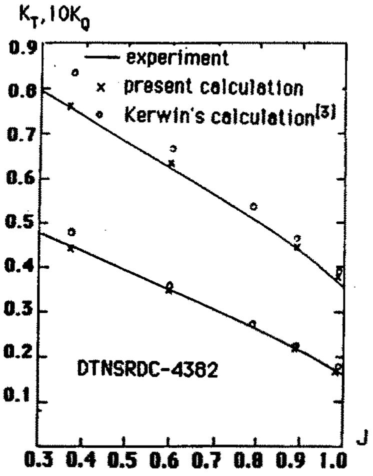

Fig. 5 Comparison of Open—Water Characteristics for DTNSRDC Propeller 4382

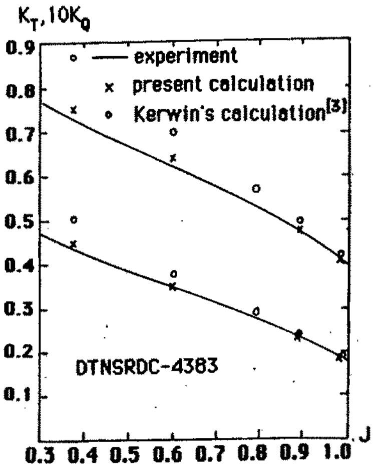

Fig. 6 Comparison of Open—Water Characteristics for DTNSRDC Propeller 4383

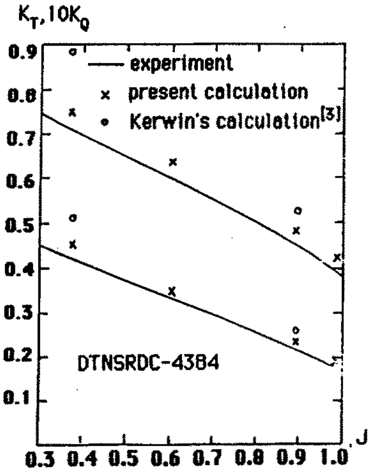

Fig. 7 Comparison of Open—Water Characteristics for DTNSRDC Propeller 4384

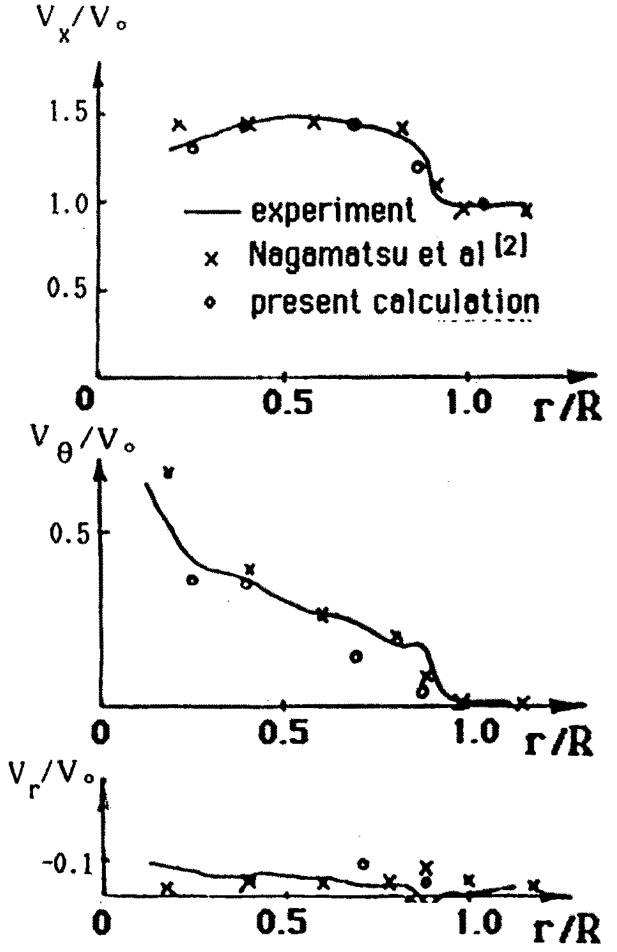

Fig. 8 Velocity Field behind DTNSRDC Propeller 4381 (J=0.889,X/D=0.5)

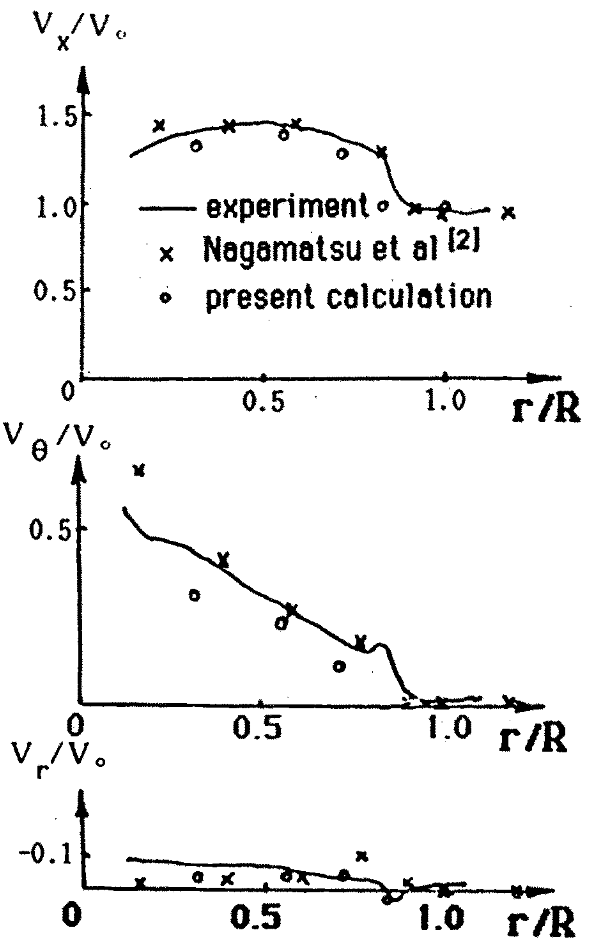

Fig. 9 Velocity Field behind DTNSRDC Propeller 4383 (J=0.889,X/D=0.5)

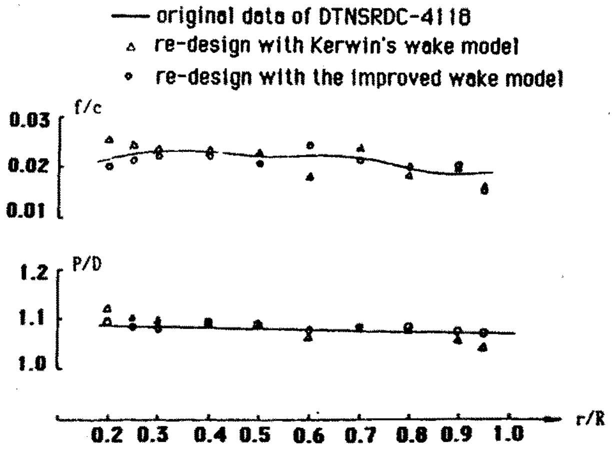

Fig. 10 Pitch and Camber Distribution

Table 1 Comparison of Re—design Results for DTNSRDC Propeller 4383

	original data of DTNSRDC-4383		Kerwin's wake model K_T=0.2366, K_Q=0.0467		the improved wake model K_T=0.2280, K_Q=0.04618	
r/R	P/D	f/c	P/D	f/c	P/D	f/c
0.2	1.566	0.0402	1.559	0.0388	1.620	0.0407
0.25	1.539	0.0408	1.502	0.0384	1.553	0.0408
0.3	1.512	0.0407	1.471	0.0373	1.517	0.0401
0.4	1.459	0.0385	1.423	0.0349	1.461	0.0377
0.5	1.386	0.0342	1.346	0.0314	1.378	0.0337
0.6	1.298	0.0281	1.272	0.0256	1.293	0.0285
0.7	1.198	0.0230	1.172	0.0216	1.191	0.0229
0.8	1.096	0.0189	1.081	0.0175	1.093	0.0193
0.9	0.996	0.0159	0.985	0.0147	0.994	0.0155
0.95	0.945	0.0168	0.934	0.0113	0.945	0.0117

(4118, 4383) are re—designed for the purpose of evaluating the feasibility of present method and investigating the influence of trailing vortex wake model. The calculated pitch and camber distributions are compared with the original data. Numerical results show that the two wake models make no difference in conventional propeller design (DTNSRDC—4118). Both of them give satisfactory propellers. However, it can be found that the new wake model obtains better result for highly skewed propeller design. The propeller designed by numerical lifting—surface theory with Kerwin's wake model has lower pitch and camber distribution. It means that the propeller will absorb lower power than that of design condition in practical operation.

The calculated pitch and camber distributions together with original data of DTNSRDC propeller 4118 is shown in Fig. 10. The pitch and camber distributions of re—designed propeller 4383 which has a torque coefficient almost identical with the required value are listed in Table 1.

Finally, it should be mentioned that the new wake model has been extended to the prediction of unsteady marine propeller performance successfully [6].

References

[1] Wang Guoqiang and Hu Shougen, :"Improvement of Prediction Method for Propeller Characteristics and Blade Pressure Distribution", Predetermined to be published by the Journal of Shipbuilding of china No. 100, 1988, also Selected Papers of the CSNAME, vol.3, 1987—1988

[2] Nagamatsu, N, and Shimizu, H., : "Study on Propeller Slipstream", Journal of the KANSAI Society of Naval Architects. Japan, No. 197, 1985

[3] Kerwin, J. E. and Lee, C—S, : "Prediction of Steady and Unsteady Marine Propeller Performance by Numerical Lifting—Surface Theory", Trans. of SNAME vol.86, 1978

[4] Maruo, H., Ikehata, M. and Ando, M., "Theoretical Prediction of Unsteady Propeller Characteristics in the Non—Uniform Wake Field", 15th Symposium on Naval Hydorodynamics, 1984

[5] Wang Guoqiang, Yang Chenjun and Hu Shougen, :"Investigations into the Theoretical Design Methods for Highly Skewed Propeller", Accepted by the Symposium on Celebrating the 75th Anniversary of Birth of Prof. Xin Yixin, December, 1987

[6] Wand Guoqiang and Hu Shougen, :"Prediction of Unsteady Propeller Performance by Numerical Lifting—Surface Theory", to be published

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PR—7

GILBERT DYNE
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Göteborg, Sweden

WRITTEN DISCUSSION TO THE 18TH ITTC PROPULSOR COMMITEE REPORT

Let me first congratulate the committee to an instructive and valuable report.

Many investigators have criticised Betz' and Lerbs' optimum critria, claiming that it is possible to get still higher efficiencies by increasing the tip load. If this be true, the proofs given by Betz and Lerbs must be wrong. But as far as I know nobody has so far shown that this is the case. It is a pity that not even the committee has discussed the validity of these proofs since if they are correct. Then an increased tip load will not give any improvements. But if, on the other hand, they are wrong then it would be easy to correct the errors and get more reliable optimum criteria.

The criticism is instead based upon results obtained by non−linear theories for moderately−loaded propellers, where the propeller induced velocities are computed by the law of Biot−Savart. It is easy to show that the axial induced velocity Ua is sensitive to the pitch of the outmost vortices especially when the tip load is high. Since Ua directely influences the torque but not the thrust, an error in tip vortex pitch automatically means an error in the efficiency.
For analytic methods there is a singularity with infinite velocities at the tips which must influence the tip−vortex pitch. In the calculation methods used, this singularity is either ignored or avoided by stopping the calculations at a small distance from the tips. In either case, the tip−vortex pitch will be wrong. These methods can therefore not be used to check the optimum criteria.

In the future when improved non−linear calculation theories have been developed the situation will be different, as pointed out by the committee.
But also in this case the calculations must be carried all the way to the blade tips to give reliable efficiency values. It is uncertain in panel methods can be used for this purpose.

PR−8

H. JARZYNA
Ship Propeller Department
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ON THE PROPELLER GEOMETRY ON THE LOAD OPTIMIZATION−PROPELLER HULL INTERACTION

I would first like to compliment the Propulsor committee on the most interesting Report.

Propeller geometry:
Following the recommendations of the XVII ITTC Propeller Committee I have sent to all Committee Members my paper concering the screw geometry nomenclature. I would like to thank the Propulsor Committee Members for their consideration of my proposals. All proposals but one have been accepted and included in the Report.

One proposal has been left out of account. It seems to me however that it is worth to be mentioned here. The definition of "warp" is concerned.

In the ITTC Dictionary in English version we can find under "warp" that it is synonymous with skew angle, and under "skew angle" we can read among other sentences the following: "This angle is the same as the warp". In the literature one can find quite another concept of warp that does not correspond with that in the dictionary. My proposal is to change the definition of warp in the ITTC Dictionary in agreement with the concept which can be found in the publications.
Warp is the particular combination of rake and skew giving the total axial displacement of the reference point of any propeller blade section equal zero

$$\theta_S(r)\cdot\tan\phi(r)+\tan\theta_R(r)=0$$

Local wap measure is the local skew angle Q_S (r), or the local rake angle θ_R (r) .
Measuring warp can be done by measuring skew, or measuring rake, used in warping. The local warp angle is allways equal to the local skew angle used in warping. The local skew angle θ_S (r) is the most practical measure of warp. The rake, being the element of warping, is then determined from the definition equation

$$\tan\theta_R(r)=-\theta_S(r)\cdot\tan\phi(r)$$

Warp angle extent is equal to the skew angle extent used in warping.

Load optimization. Propeller hull interaction.

My second comment is in connection with the third part of the Committee Report under the title "Load optimization". In this part my paper, related to this problem, is shortly characterized. The fundamental idea of this paper is however overlooked. I would like to present this idea.
The interaction between the ship hull and propeller results, among others, in the resistance changes and the thrust changes. The local changes of these two quantities are of special interest.
When speaking about changes the reference values must be determined. The changes of the ship resistance, local or total, are related to the ship resistance Ro, when towing the ship without propeller. The changes of the propellers thrust are to be related, in my opinion, to the thrust of an hypothetical propeller, which is realizing the needed motion, without any interaction with the hull.
This reference propeller, denoted with the index "∞", is working in the uniform νs, produces the thrust $T_\infty = Ro$.
The propellr diameter is $D_\infty = D_B$, the revolutions per second $\eta_\infty = \eta_{opt}$ being the optimum revolutions for the given diameter $D_\infty = D_B$. The thrust radial distribution $\alpha T_\infty = f_\infty(r)$ is the optimum one in the uniform flow, νs. The total and local changes of ship resistance and propeller thrust due to interaction may now be determined

change of resistance

$$\frac{R_B - R_o}{R_B} = t_R$$

$$\frac{dR_B(\vec{r}) - dR_o(\vec{r})}{dR_B(\vec{r})} = t_R(\vec{r})$$

change of thrust

$$\frac{T_B - T_\infty}{T_B} = t_T$$

$$\frac{dT_B(x) - dT_\infty(x)}{dT_B(x)} = t_T(x)$$

The position vector $\vec{r}$, related to the hull surface point, and the nondimensional coordinate x, related to the propeller disc radius, play only role of denoting the position, where the changes take place.
The resistance changes are characterized by the suction coefficients, total t_R, and local $t_R(\vec{r})$.
The thrust changes are characterized by the thrust change coefficients or thrust deduction factors, total t_T or local $t_T(x)$.
In the case when $R_o = T_\infty$ and $R_B = T_B$ (υ_s=const) there is allways

$$t_R = t_T = t$$

and there is no need to distinguish between the total suction coefficient and the total thrust deduction factor.
Therefore in the definition formulas changing the quantities (R_B and T_B) gives the well known definition

$$t = \frac{T_B - R_o}{T_B}$$

The local quantities are allways different, also in the special case υ_s=const

$$t_R(\vec{r}) \neq t_T(x)$$

The local suction coefficient is allways different from the local thrust coefficient. There is no possibility to change the resistance elements $dR_B(\vec{r})$ with the thrust elements $dT_B(x)$ in the definition formulas of the local coefficients.
The local suction coefficient can be defined using only the resistance elements, the thrust deduction coefficient with thrust elements in definition can only be used for thrust changes description.

$$1 - t_T(x) = \frac{dT_\infty(x)}{dT_B(x)}$$

This new definition after being introduced creates new possibilities to investigate the interaction mechanisms.
— When optimizing the radial distribution of

propeller blade circulation in behind condition from the point of view of propulsion efficiency one can find in the paper dealing with this problem a secret expression $(1-t_x)$ or $(1-t\ (r)\ )$. I did not find anywhere the definition of this expression. I could not find paper where this expression is included into the optimizing process to be active in determination of the optimum radial distribution of the blade circulation.

It is a dead term in the whole optimizing process. In the end results this term $(1-t_x)$ or $[1-t\ (r)\ ]$ is replaced freely by a constant value $(1-t_x)$, the total suction coefficient, or by a function f (x) related to the radial distribution of the local wake fraction w_x.

The substance of this secret term may be identified however univocally with the new definition given above

$$1-t_x=1-t_T(x)=\frac{dT_\infty(x)}{dT_B(x)}$$

Therefore the definition of the, up to day underfined, expression is given.

One can prove, this was done in my paper published in ISP, that the criteria for optimizing the radial thrust distribution using the underfined term $(1-t_x)$ are all internally inconsistent. This internal inconsistency will be removed after replacing the term $1-t_x$ by the new definition of thrust chage coefficient,

$$1-t_T(x)=\frac{dT_\infty(x)}{dT_B(x)}.$$

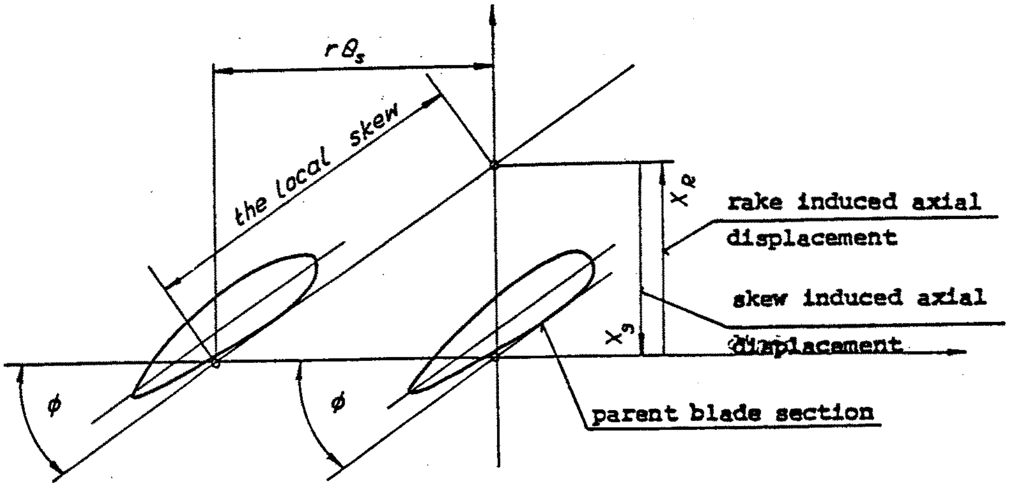
The internal consistency is grounded on the demand, that any criterion built on the general assumption of constancy of a quantity, say A, has to satisfy the requirement A = const in its resulting form.

This demand is not fulfilled by the criteria being in use.

—One can see that the thrust change coefficient or thrust deduction factor proposed to be used in

Warp ≡ rake + skew with the demand that the condition $x_R(r)+x_s(r)=0$ is satisfed for each $r\ (r_h \leqslant r \leqslant R)$

(Measuring warp one can measure skew and calculate rake or one versa)



$$X_R\ (r) = r \tan\theta_R(r) \qquad X_R+X_S = \tan\theta_R(r)+\theta_S(r)\tan\phi\ (r)=0$$

$$X_S\ (r) = r\theta_S(r)\cdot\tan\phi(r) \qquad \tan\theta_R\ (r)=-\theta_S(r)\cdot\tan\phi\ (r)$$

Fig. 1 Warping the Parent Blade

optimizing process is a functional dependent upon the function $\frac{dG(x)}{dx}$ and the coordinate

$$1-t_T(x)=f[x, \frac{dG(x)}{dx}]$$

and takes therefore an active part in this optimizing process. After the function G (x) has been deteremined, G $(x)=G_{opt}(x)$ the functional

$1-t_T(x)=f[x, \frac{dG(x)}{dx}]$ takes the shape of

the function of x only

$$1-t_T(x)=f[x, \frac{dG_{opt}(x)}{dx}]=f_T(x)$$

— Systematic calculations over some sets of ship hull families, with different stern shapes and different propeller loading, result in one function $[1-t_T(x)]$ for each case tested in computing process. A set of resuts related to different hull shapes, different radial distributions of $(1-Wx)$, different propeller loadings can be the basis for an regressive analysis which can lead to the general relation between $[1-t_T(x)]$ and $(1-wx)$

$$[1-t_T(x)]=f[(1-w_x), C_T]$$

It seems to me, that further investigations, using the new defined quantity, are included indeed in the conclusion 1.3 of the Report.

PR-9

GU MAO XIANG
CSSRC, Wuxi, China

ON DRAFT RECOMMENDATIONS OF THE PROPULSOR COMMITTEE

In view of the importance of new blade sections research as discussed by Dr. Petersen for the design of advanced propulsors. I would like to ask the Committee to stress in its draft recommendations to the Conference or next Committee that more intensive reserach in this direction should be encouraged.

PR-10

K. R. SUHRBIER
Vosper Thornycroft (UK) Ltd, Portsmouth, U. K.

ON THE REPORT OF THE PROPULSOR COMMITTEE

The Committee is to be congratulated on a very interesting and informative report. I would like to comment briefly on two points.

With regard to the calculation of propeller-induced pressure fluctuations discussed in Section II.2, I think it may be worth emphasizing that we should, in general, still be careful when comparing the various data. The reference information, i.e. model and/or full scale results, is for a number of reasons frequently not as reliable as we may wish to believe. This can also be concluded from data given in the Report of the Cavitation committee.

As to improved nomenclature for propeller geometry (p.141/142) , clear definitions are certainly most important, in particular for skewed propellers. Different definitions are currently in use, especially for rake, and misinterpretations can have rather expensive consequences. I therefore welcome the Committee's attempt to address this problem and like to suggest to include in the reply a sketch illustrating the new pro-communication between designers and manufacturers.

II. REPLY BY THE PROPULSOR COMMITTEE

The Propulsor Committee thanks **Professor Jarzyna (PR−8)** not only for his discussion, but also for his previous proposals concerning screw geometry nomenclature. We agree with the proposal for the definition of warp:

> Warp is the particular combination of rake and skew that produces a zero value for the total axial displacement of the reference point of a propeller blade section.

According to this definition it is not necessary, and sometimes confusing to define the measure of local warp, because warping is a property of the entire blade and not of a particular section. Hence it is preferable to define warp as just given and to speak about "warped propellers", but we have reservations about a warp measure. The propeller geometry can be defined adequately with skew and rake measures.

In Professor Jarzyna's second point, he calls attention to variable interaction coefficients in SHP minimization, and specifically addresses a variable thrust deduction fraction. We agree that this coefficient is strictly variable and so noted in the Report on p. 111. However the variation is generally minor and, proper treatment is complex, so in the Report we continued our discussion with an assumption that it was satisfactory to take it as a constant, in order to explicitly examine the major dependence on propeller circulation distribution. We thank Professor Jarzyna for his thoughtful comments and hope that in the future we will see numerical calculations that specifically address this variable quantity.

Professor Dyne (PR−7) draws attention to the importance of the mathematical model employed in the extremum problem, particularly the shed vortex sheet geometry in the tip region. We agree on its importance and look forward to a day when an improved specification can be defined (say from detailed flow field measurements in the tip region) so that realistic and satisfactory solutions are predicted. Until such time, we found that the numerically predicted efficiency of tip−loaded propellers is greater when computed with the non−linear (but restricted) moderately−loaded mathematical model of the shed vortex sheet, than when predicted by the criteria based on the linear model of light−loading employed by Betz, Lerbs, van Manen, etc. However, we are not at all convinced that increased tip loads must, or will, be appropriate in real flow. We thank Professor Dyne for emphasizing this point.

Professor Wang (PR−5,6) describes two undertakings that include a non−linear shed vortex sheet model. We are pleased to see this initial description of his work and look forward to reading the full report.

Dr.'s Hadjimikhalev, Haimov and Yosifov (PR−1) provide additional information relative to the comparative calculations of propeller design. Results for their method I belong to group 1 in the Committee Report and are in agreement with conclusion 5.3.2 (1) on page 133 of Volume I. Unfortunately, results from their method II are difficult to compare with that in the committee report because of the difference in circulation distributions. It is the Committee's understanding that the conclusions in their discussion are essentially in agreement with that of 5.3.2 in the Committee report. The comparative calculations of design initiated by the Committee were aimed at surveying the differences between results based on various analytical/numerical models. For practical design purposes, certain empirical corrections should be used depending on the experience of individual designers. Here BSHC has presented their own experience and we thank them. Another point in their discussion concerned the evaluation of singular integrals. Based on the results derived by Muskhilishvili for integrals with a Cauchy singularity, it is mentioned in the Committee Report that the induced−velocity calculations may have a singular behavior at the ends when the induction−factor method is used and considers that behavior to be of concern. However, we have not yet reviewed the reference quoted in the discussers' contribution, so do not make further comments.

Dr. Kuiper (PR−2) raised two questions; the first one is on pressure calculations, and the second is on the lifting surface correction.

As Dr. Kuiper points out in the first part of his discussion, the scatter of the various effective velocity distributions used in the comparative cavitation study is considerable. We agree that the error is likely caused by the inaccuracy in computing the potetial component of the field−point velocity induced by the propeller. We had hoped to receive enough replies to the questionnaire to distinguish between use of nominal velocity and total velocity as the basis for effective velocity computations. Since few organizations carried out such calculations, this was not possible. It is clear that work should continue on computing the effective wake. More information on the comparative study may be found in a report prepared by Committee Member C.−S. Lee, which was available at the Conferece and can now be obtained from him.

Dr. Kuiper provides his experience with propeller design calculations, using lifting−line analysis and lifting−surface correction factors. The Committee notes that shape comparisons are generally made with the same camber ratio value at each blade section and, therefore, the camber distribution often appears to be close to that from lifting−surface analysis in the case of symmetry. However, the problem is that the camber ratios obtained from explicit calculations with lifting−line theory combined with lifting−surface correction factors may significantly deviate from those by lifting surface analysis for many designs similar to that shown in figure 17 in the Committee Report.

In response to the discussions by **Messrs. Hoshino and Sasajima (PR−3)**, the Propulsor Committee fully agrees with their suggestion. The pressure distribution on the propeller blade operating in non−uniform flow is clearly the most important information to start the prediction of cavitation on the blades. But as reported by the 17th ITTC Propeller Committee, the comparative study of pressure distribution on a propeller blade in uniform flow led to the conclusion that current efforts were not satisfactory: the scatter between computed results by various participants was large. We have yet to make a similar evaluation of the capability to predict the unsteady pressure distribution on the propeller blade, but we do not expect improvement. In addition, the Committee has reservations about an ability to predict the effective wake at points on the blade in unsteady flow. Although we tried to identify the sources of error in predicting pressure fluctuations on the hull, the situation is not fully understood and we thank the discussers for their suggestions and comments.

The comments of **Dr. Peterson (PR−4)** are appreciated as they inform the Conference about activities under auspices of the Propulsor Committee and shared with the Cavitation Committee. For formal and practical reasons the results of the activities could not be included in the Report. Practical reasons have to do with the time schedule of the round table discussions. Formal reasons have to do with the responsibility of a technical committee for reviewing new findings, certainly in an emerging technology. The Committee has given due attention to these kinds of situations in recommendation 5 to the conference and recommendation 7 for work of the future committee. We request the Conference note the implications of increased activities in round table discussions and workshops. Dr. Peterson's comments are an enhancement of the finding contained in the Report and emphasize some points which need further investigation, and are thus welcomed by the Committee.

Mr. Suhrbier (PR−10) notes that the reliability of pressure pulse data is often not as great as desired and we agree. For his second point, we respond to his request for a clear definition of the propeller geometry with the enclosed sketch, a modification of the sketch (Fig. 20) in the ITTC Dictionary of Ship Hydrodynamics.

We thank the discussers for their clarifying questions, suggestion and progress reports.

Ⅲ. COMMITTEE REPORT ERRATA

In Table 11, p.139, of the Committee Report, the third and fourth entries in the right−hand column should read 55 (not 160) and 160 (not 161) respectively.

Cited references not included in the bibliography include:

CHATTOPADHYAY SUDEB, "Study of Cavitation on High Speed Propellers in Oblique Flow", Ph. D. Thesis, University of Tokyo, Department of Naval Architecture, July, 1985. (Cited on page 125 of Volume Ⅰ)

ABBISS, J. B., et al., "Experiments using a Three−Component Laser−Anemometry System on a Subsonic Flow with Vorticity,", U. S. Air Force TR 80081, 1980. (Cited on page 126 of Volume Ⅰ)

Fig. 20 (a, b and c) Diagrams Showing Recommended Reference Lines and Terminology or Propeller Geometry

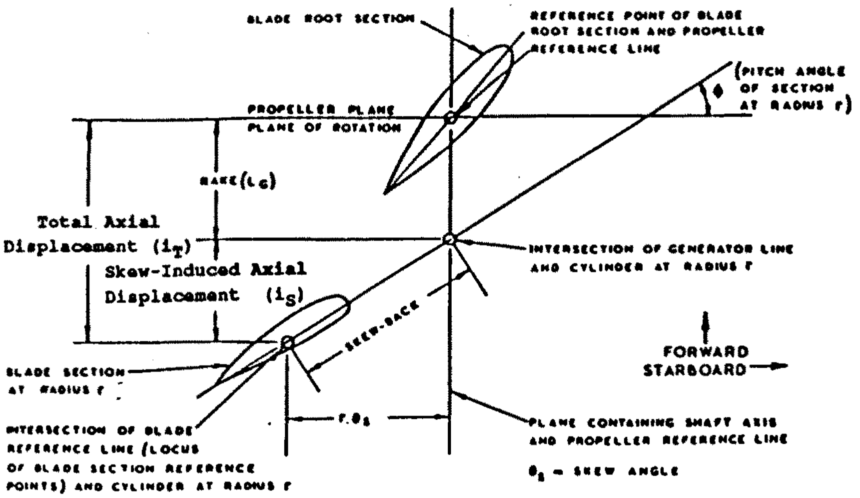
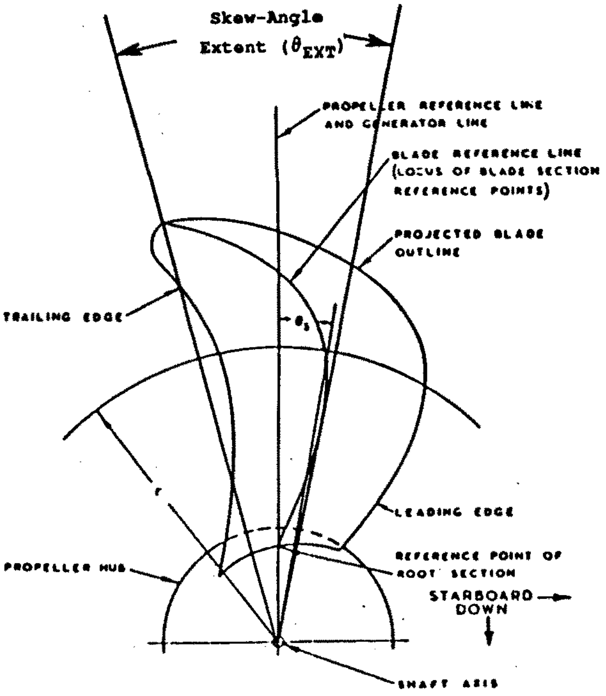


Fig. 20 (a) View of Unrolled Cylindrical Sections at Blade Root and at any Radius of a Right—handed Propeller (looking down) Showing Recommended Location of Propeller Plane.



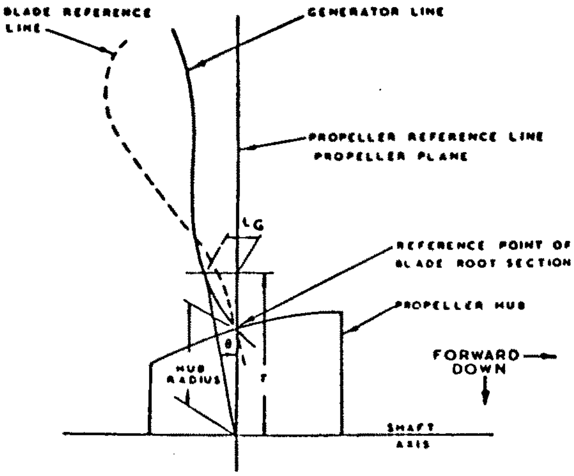


Fig. 20 (b) Diagram Showing Recommended Reference Lines (looking to port)

Fig. 20 (c) Diagram Showing Recommended Reference Lines (looking forward)