A STATISTICAL RE-ANALYSIS OF RESISTANCE AND PROPULSION DATA

by

J. Holtrop*

1. Introduction

In a recent publication [1] a power prediction method was presented which was based on a regression analysis of random model and full-scale test data. For several combinations of main dimensions and form coefficients the method had been adjusted to test results obtained in some specific cases. In spite of these adaptations the accuracy of the method was found to be insufficient for some classes of ships. Especially for high speed craft at Froude numbers above 0.5 the power predictions were often wrong. With the objective to improve the method the data sample was extended covering wider ranges of the parameters of interest. In this extension of the data sample the published results of the Series 64 hull forms [2] have been included. The regression analyses were now based on the results of tests on 334 models. Beside these analyses of resistance and propulsion properties a method was devised by which the influence of the propeller cavitation could be taken into account. In addition some formulae are given by which the effect of a partial propeller submergence can tentatively be estimated. These formulae have been derived in a study carried out in a MARIN Co-operative Research programme. Permission to publish these results is gratefully acknowledged.

2. Re-analysis of resistance test results

The results were analysed using the same sub-division into components as used in [1]:

$$R_{\text{Total}} = R_F(1+k_1) + R_{APP} + R_W + R_B + R_{TR} + R_A$$

where:

- R_F = frictional resistance according to the ITTC-1957 formula
- $1+k_1$ = form factor of the hull
- R_{APP} = appendage resistance
- R_W = wave resistance
- R_B = additional pressure resistance of bulbous bow near the water surface
- R_{TR} = additional pressure resistance due to transom immersion
- R_A = model-ship correlation resistance.

A regression analysis provided a new formula for the form factor of the hull:

$$1+k_1 = 0.93 + 0.487118\, c_{14} (B/L)^{1.06806} (T/L)^{0.46106} (L/L_R)^{0.121563} (L^3/\nabla)^{0.36486} (1-C_P)^{-0.604247}.$$

In this formula B and T are the moulded breadth and draught, respectively. L is the length on the waterline and ∇ is the moulded displacement volume. C_P is the prismatic coefficient based on the waterline length. L_R is defined as:

$$L_R = L(1 - C_P + 0.06 C_P\, lcb/(4C_P - 1))$$

where lcb is the longitudinal position of the centre of buoyancy forward of 0.5 L as a percentage of L.
The coefficient c_{14} accounts for the stern shape. It depends on the stern shape coefficient C_{stern} for which the following tentative figures can be given:

Afterbody form	C_{stern}
Pram with gondola	-25
V-shaped sections	-10
Normal section shape	0
U-shaped sections with Hogner stern	10

$$c_{14} = 1 + 0.011\, C_{\text{stern}}$$

As regards the appendage resistance no new analysis was made. For prediction of the resistance of the appendages reference is made to [1].

A re-analysis was made of the wave resistance. A new general formula was derived from the data sample of 334 models but calculations showed that this new prediction formula was not better in the speed range up to Froude numbers of about $F_n = 0.5$. The results of these calculations indicated that probably a better prediction formula for the wave resistance in the high speed range could be devised when the low speed data were left aside from the regression analysis.
By doing so, the following wave resistance formula was derived for the speed range $F_n > 0.55$.

$$R_{W-B} = c_{17} c_2 c_5 \nabla \rho g \exp\{m_3 F_n^d + m_4 \cos(\lambda F_n^{-2})\}$$

where:

$$c_{17} = 6919.3\, C_M^{-1.3346} (\nabla/L^3)^{2.00977} (L/B-2)^{1.40692}$$

$$m_3 = -7.2035 (B/L)^{0.326869} (T/B)^{0.605375}.$$

The coefficients c_2, c_5, d and λ have the same definition as in [1]:

*) Maritime Research Institute Netherlands, Wageningen, The Netherlands.

$c_2 = \exp(-1.89\sqrt{c_3})$

$c_5 = (1-0.8A_T/(BTC_M)$

$\lambda = 1.446C_P - 0.03L/B$
when $L/B < 12$

$\lambda = 1.446C_P - 0.36$
when $L/B > 12$

$d = -0.9$

$c_3 = 0.56A_{BT}^{1.5}/\{BT(0.31\sqrt{A_{BT}} + T_F - h_B)\}$

$m_4 = c_{15}\, 0.4 \exp(-0.034F_n^{-3.29})$

$c_{15} = -1.69385$
when $L^3/\nabla < 512$

$c_{15} = -1.69385 + (L/\nabla^{1/3} - 8)/2.36$
when $512 < L^3/\nabla < 1726.91$

$c_{15} = 0$
when $L^3/\nabla > 1726.91$

The midship section coefficient C_M and the transverse immersed transom area at rest A_T and the transverse area of the bulbous bow A_{BT} have the same meaning as in [1]. The vertical position of the centre of A_{BT} above the keel plane is h_B. The value of h_B should not exceed the upper limit of 0.6 T_F.

Because attempts to derive prediction formulae for the wave resistance at low and moderate speeds were only partially successful it is suggested to use for the estimation of the wave resistance up to a Froude number of 0.4 a formula which closely resembles the original formula of [1]. The only modification consists of an adaptation of the coefficient that causes the humps and hollows on the resistance curves. This formula, which is slightly more accurate than the original one reads:

$$R_{W-A} = c_1c_2c_5\ \nabla\rho g \exp\{m_1F_n^d + m_4\cos(\lambda F_n^{-2})\}$$

with:

$c_1 = 2223105\, c_7^{3.78613}(T/B)^{1.07961}(90-i_E)^{-1.37565}$

$c_7 = 0.229577(B/L)^{0.33333}$
when $B/L < 0.11$

$c_7 = B/L$
when $0.11 < B/L < 0.25$

$c_7 = 0.5 - 0.0625L/B$
when $B/L > 0.25$

$m_1 = 0.0140407L/T - 1.75254\nabla^{1/3}/L - 4.79323B/L - c_{16}$

$c_{16} = 8.07981C_P - 13.8673C_P^2 + 6.984388C_P^3$
when $C_P < 0.8$

$c_{16} = 1.73014 - 0.7067C_P$
when $C_P > 0.8$

m_4 : as in the R_W formula for the high speed range.

For the speed range $0.40 < F_n < 0.55$ it is suggested to use the more or less arbitrary interpolation formula:

$$R_W = R_{W-A_{0.4}} + (10F_n - 4)(R_{W-B_{0.55}} - R_{W-A_{0.4}})/1.5$$

Here $R_{W-A_{0.4}}$ is the wave resistance prediction for $F_n = 0.40$ and $R_{W-B_{0.55}}$ is the wave resistance for F_n = 0.55 according to the respective formulae.

No attempts were made to derive new formulations for the transom pressure resistance and the additional wave resistance due to a bulb near the free surface. The available material to develop such formulae is rather scarce. As regards the height of the centre of the transverse bulb area h_B it is recommended to obey the upper limit of 0.6 T_F in the calculation of the additional wave resistance due to the bulb.

3. Re-analysis of propulsion data

The model propulsion factors and the model-ship correlation allowance were statistically re-analysed using the extended data sample. This data sample included 168 data points of full-scale trials on new built ships. In the analysis the same structure of the wake prediction formulae in [1] was maintained. By the regression analyses new constants were determined which give a slightly more accurate prediction.

A point which has been improved in the wake prediction formula is the effect of the midship section coefficient C_M for full hull forms with a single screw.

The improved formula for single screw ships with a conventional stern reads:

$$w = c_9c_{20}C_V\frac{L}{T_A}\left(0.050776 + 0.93405\,c_{11}\frac{C_V}{(1-C_{P1})}\right) + 0.27915\,c_{20}\sqrt{\frac{B}{L(1-C_{P1})}} + c_{19}c_{20}$$

The coefficient c_9 depends on the coefficient c_8 defined as:

$c_8 = BS/(LDT_A)$
when $B/T_A < 5$

or

$c_8 = S(7B/T_A - 25)/(LD(B/T_A - 3))$
when $B/T_A > 5$

$c_9 = c_8$
when $c_8 < 28$

or

$c_9 = 32 - 16/(c_8 - 24)$
when $c_8 > 28$

$c_{11} = T_A/D$
when $T_A/D < 2$

or

$c_{11} = 0.0833333(T_A/D)^3 + 1.33333$
when $T_A/D > 2$

$c_{19} = 0.12997/(0.95 - C_B) - 0.11056/(0.95 - C_P)$
when $C_P < 0.7$

or

$c_{19} = 0.18567/(1.3571 - C_M) - 0.71276 + 0.38648\, C_P$ when $C_P > 0.7$

$c_{20} = 1 + 0.015\, C_{stern}$

$C_{P1} = 1.45\, C_P - 0.315 - 0.0225\, lcb$.

The coefficient C_V is the viscous resistance coefficient with

$$C_V = (1+k)\, C_F + C_A$$

As regards the thrust deduction of single screw ships a new formula was devised of comparable accuracy:

$$t = 0.25014(B/L)^{0.28956}\,(\sqrt{BT}/D)^{0.2624}\, / \,/(1 - C_P + 0.0225\, lcb)^{0.01762} + 0.0015\, C_{stern}$$

For the relative-rotative efficiency an alternative prediction formula was derived but because its accuracy is not better than that of the original one it is suggested to use the prediction formula of [1]:

$$\eta_R = 0.9922 - 0.05908\, A_E/A_O + 0.07424(C_P - 0.0225\, lcb)$$

For multiple-screw ships and open-stern single-screw ships with open shafts the formulae of [1] were maintained.

The model-ship correlation allowance was statistically analysed. It appeared that for new ships under ideal trial conditions a C_A-value would be applicable which is on the average 91 per cent of the C_A-value according to the statistical formula of [1]. Apparently, the incorporation of more recent trial data has reduced the average level of C_A somewhat. It is suggested, however, that for practical purposes the original formula is used.

4. The influence of propeller cavitation and partial propeller submergence

Especially on high speed craft propeller cavitation can effect the propulsive performance.

Tests on B-series propellers in uniform axial flow under cavitating conditions were reported in [3], but the representation of the results was confined to a graphical form only.

The $K_T - K_Q - J$ relationship of the 16 B-series propellers tested under cavitating conditions were fed into the computer for a statistical analysis. The data used consisted of the changes of K_T and K_Q due to cavitation at certain J-values. The unaffected K_T and K_Q values of the propellers were supposed to be determined accurately by the polynomials given in [4] and [5]. From preliminary analyses it appeared that for each propeller the conditions where influence of the suction-side cavitation begins can be represented well by a certain value of the speed-independent coefficient:

$$\frac{K_T}{J^2 \sigma_o} = \frac{T}{2D^2(p_o - p_v + \rho g h)}$$

This coefficient is indicated as $(K_T/(J^2\sigma_o))_{BI}$.

Here K_T is the thrust coefficient, J is the advance coefficient and σ_o is the cavitation number defined as

$$\sigma_o = \frac{p_o - p_v + \rho g h}{\frac{1}{2}\rho V^2} ,$$

where p_v is the vapour pressure, $p_o + \rho g h$ is the static pressure in the undisturbed flow at the level of the shaft centre line, ρ is the density of the water and V is the advance speed of the propeller.

From the data of the B-series $(K_T/(J^2\sigma_o))_{BI}$ was determined for each propeller and by means of multiple regression analysis these $(K_T/(J^2\sigma_o))_{BI}$ values were correlated to the main propeller parameters. This resulted into the following formula:

$$(K_T/(J^2\sigma_o))_{BI} = 0.06218 + 0.1194 A_E/A_O - 0.00249\, Z$$

Here A_E/A_O is the expanded blade area ratio and Z is the number of blades.

The pitch ratio appeared to have no significant influence on the $K_T/(J^2\sigma_o)$ value where cavitation begins to affect the propulsive performance. Of course, this will not be true for the effect of the pitch setting of a controllable-pitch propeller because then the radial load distribution is changed.

If $K_T/(J^2\sigma_o)$ exceeds the value given by the prediction equation cavitation influence is present and should be accounted for. This influence was represented in relation to the characteristics of the non-cavitating propeller because these are well defined by the polynomial representation in [4] and [5]. This was done by analysing the ratios

$$F_N = \left(\frac{1}{J}\right)_{\sigma_o} \Big/ \left(\frac{1}{J}\right)_{\sigma_o = \infty}$$

and

$$F_P = (K_Q/J^3)_{\sigma_o} / (K_Q/J^3)_{\sigma_o = \infty}$$

Coefficient F_N is the factor by which the rotation rate n should be increased, whereas F_P is the factor by which the propulsive power is increased due to cavitation. The factors F_N and F_P were considered as a function of K_T/J^2 for each cavitation number because K_T/J^2 can be regarded the same for non-cavitating conditions and for conditions in which the propulsive properties are affected.

It appeared that the influence of the cavitation number could be expressed well by using

$$K_T/(J^2\sigma_o)$$

as an independent variable.

By means of selective regression analysis the proportionality was correlated with the main propeller particulars, and the following prediction equations were derived:

$$F_N = 1 + 46.4301\,(A_E/A_O)^{-1.746}(10-Z)^{-2.223}\left(\frac{K_T}{J^2\sigma_o} - \left(\frac{K_T}{J^2\sigma_o}\right)_{BI}\right)^{1.2}$$

and

$$F_P = 1 + 15.1845\,(A_E/A_O)^{-2.2514}(10-Z)^{-1.4478}\left(\frac{K_T}{J^2\sigma_o} - \left(\frac{K_T}{J^2\sigma_o}\right)_{BI} - 0.01\right)^{1.2}$$

It should be noted, however, that the scatter in the data was fairly large. It is suggested that the parameters A_E/A_O and Z are not used outside the ranges of

$$0.75 < A_E/A_O < 1.05 \quad \text{and} \quad 4 \leqslant Z \leqslant 5$$

The formula for F_N is valid for

$$\frac{K_T}{J^2\sigma_o} \geqslant \left(\frac{K_T}{J^2\sigma_o}\right)_{BI}$$

whereas the formula for F_P is valid only for

$$\frac{K_T}{J^2\sigma_o} \geqslant \left(\frac{K_T}{J^2\sigma_o}\right)_{BI} + 0.01$$

In all other cases F_N and F_P are 1.0.

In the optimization of the performance of ships in ballast conditions the behaviour of not fully immersed propellers can be of importance.

For practical use the following equations were derived from model experiments on the assumption that by introducing a fictitious increase G of the entrance velocity the influence of the partial emergence can be accounted for over the range of propeller loadings of interest:

$$V_E = V(1-w)\,G$$

V_E is the resultant entrance velocity of the propeller. This increase-factor G was related to coefficients describing the emergence of the propeller and the propeller loading.

As a parameter indicating the emergence the variable U is used with:

$$U = \frac{D + h_o - T_A - w_h}{D}$$

Where D is the diameter, h_o is the vertical distance from the keel plane to the blade tip in its lowest position, T_A is the draught aft and w_h is a measure for the wave height at the location of the propeller, approximated by:

$$w_h = 0.6\,C_B B\,c_{21}$$

where:

$$c_{21} = F_n^2 \qquad \text{when } F_n < 0.3$$

$$c_{21} = 0.09 \qquad \text{when } F_n > 0.3.$$

From experiments it appeared that the speed increase factor G could be expressed as a linear function of the emergence coefficient U and the propeller loading $K_T/J^2 = T/(\rho D^2(1-w)^2 V^2)$. Hence, for positive values of U the factor G can be determined from:

$$G = 1 + 3U\left(\frac{T}{\rho D^2(1-w)^2 V^2}\right)$$

where the coefficient 3 is an empirical constant.

When the propeller emergence is not excessive the thrust deduction and the relative-rotative efficiency can be regarded to be unaffected.

5. Numerical example

For the following hypothetical twin-screw ship the still-water powering performance is calculated over the speed range from 25 to 35 knots.

Main particulars

L	=	50.00	m	A_{BT}	=	0	
B	=	12.00	m	i_E	=	25 degrees	
T_F	=	3.10	m	C_M	=	0.78	
T_A	=	3.30	m	lcb	=	−4.5% L aft of ½ L	
∇	=	900	m^3	A_T	=	10	m^2
S_{app}	=	50	m^2	$1+k_2$	=	3	
C_{stern}	=	0		C_{WP}	=	0.80	

Related coefficients

C_P	=	0.60096		C_B	=	0.46875	
L_R	=	14.1728	m	S_{hull}	=	584.9	m^2
$1+k_1$	=	1.297		C_A	=	0.00064	
c_{17}	=	1.4133		c_5	=	0.7329	
m_3	=	−2.0298		λ	=	0.7440	
c_2	=	1.0		c_{15}	=	−1.69385	

Results resistance calculation

Speed (knots)	$m_4\cos(\lambda/F_n^2)$	$m_3F_n^d$	R_W (kN)	R_{APP} (kN)	R_{TR} (kN)	R (kN)
25	0.3279	−3.3100	475	21	25	662
27	0.1820	−3.0883	512	24	16	715
29	0.0409	−2.8962	539	28	2	756
31	−0.0834	−2.7274	564	31	0	807
33	−0.1876	−2.5780	590	35	0	864
35	−0.2730	−2.4453	618	39	0	925

Results propeller design and calculation of propulsion factors

t	=	0.054	D	=	3.231 m	η_o	=	0.705 (30 knots)
w	=	0.039	P/D	=	1.136			
η_R	=	0.980	A_E/A_O	=	0.763			

Results performance calculation

Speed (knots)	total thrust (kN)	N^* (RPM)	$P_D{}^*$ (kW)	F_N	F_P	N^{**} (RPM)	$P_S{}^{**}$ (kW)
25	699	259.3	12670	1.000	1.000	259.3	12798
27	756	275.7	14707	1.000	1.000	275.7	14856
29	799	291.1	16617	1.000	1.000	291.1	16785
31	853	307.1	18915	1.008	1.000	309.6	19106
33	913	326.2	21508	1.019	1.011	329.8	21964
35	978	340.2	24406	1.033	1.027	351.4	25318

* without effect of propeller cavitation.

** including effect propeller cavitation.

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