Extending Blade-Element Model to Contra-Rotating Configuration

Ohad Gur

1 IAI – Israel Aerospace Industries, Ben-Gurion Airport, Lod 70100, Israel

Abstract. Blade element model is one of the most popular models for rotating-wing analysis and design. Many legacy tools are based on blade-element and capable of analysing only a single-rotating configuration. The current scope suggests a simple and effective method for extending such applications to contra-rotating configurations. The model based on the classic axial-momentum theory and assumes rigid wake. The method is validated versus results from propeller, hovering rotor, and prop-fan experiments. The experiments present both single and dual-rotating wing configuration, which share the same blade geometry and test facilities. This enables the isolation of the single- versus dual-rotating configuration influences, thus validating the suggested model with high level-of-confidence. The validation shows that the same trends of the single-rotating model appears in the dual-rotating analyses. Moreover, the difference between single and dual rotating configuration is replicated accurately compared to the test results. This proves the validity of the suggested model extension. The model validation also exhibits the differences between the accuracy of the forward and rear rotating disk in the contra-rotating configuration. The rear disk contributes by the no-swirled wake and exhibits accurate results compared to the forward disk which operate similar to a single rotating wing, hence suffers from the same inaccuracies.

1. Introduction
Blade-element model (BEM) is used extensively for rotating-wing analysis and design. This includes propellers, open-rotors, wind turbines, axial compressors, fans, etc. The advantage of BEM over many others, elaborate and accurate models, such as CFD based analyses, is the combination of simple model which contains accurate 2-D aerodynamic database for the blade’s cross-sections. In that way, BEM is very simple and consumes very low computer resources. The complex effects of viscosity, compressibility, and stall, are introduced through the 2-D aerodynamic database. This is also the main draw-back of BEM – the requirement of valid, high level-of-confidence database, which suites large range of operating conditions, i.e. various Reynolds and Mach number. If such database exists, for axial design condition BEM exhibits excellent match to experimental results. [1] These advantages enable researchers to use BEM for prop-fan and open-rotor analysis and design. In most cases, the validation of the model is limited, although the model is then used for vast range of cases. The current effort tries to cover these gaps.

2. Contra-rotating blade-element model
The single-rotating, SR, configurations are very common in various apparatus such as propeller, wind turbine, fan, etc. A single-rotating propeller with an axial free stream velocity, $V$, is replaced with an infinitesimal thin actuator-disk which introduces axial and circumferential momentum to the flow. The additional momentum is resulted with axial and circumferential induced velocity components, $W_a$ and $W_c$, respectively. The source of these induced velocities is the propeller thrust and torque. According to Newton’s third law, the thrust and torque that acts on the propeller is reacted by the same thrust and torque which acts on the flow. Thus, the air is accelerated to the axial direction due to the thrust and the wake is swirled due to the torque, i.e. axial and circumferential induced velocity is resulted. Note that while the flow passes the actuator disk, the axial velocity is continuous while the circumferential velocity is incontinuous and experiences a “jump” from zero to $W_c$.

A typical blade-element model is depicted as the rotating propeller’s blade is sliced to small blade-elements, each $dr$ width. The velocity components on the blade sections are summed resulting with total velocity, $U$, and local angle of attack, $\alpha$. The blade-element acts as a 2-D wing section, thus it produces lift and drag forces, $dL$ and $dD$, respectively. Summing all blade-element lift and drag components is resulted with the total thrust and torque which acts on the propeller.

In the current effort, the above blade-element model is extended to a contra-rotating case. Left part of Figure 1 shows an equivalent actuator-disks model. Each of the two actuator-disks acts the same manner as the single-rotating case. The main difference is the induced velocities. Except of the axial and circumferential induced velocities which the disk induces on itself, the disks induce velocities on each other. The rear disk induces axial cross-induced velocity on the forward disk, $W_{a,con2}$, and the forward disk induces on the rear disk both axial cross-induced velocity, $W_{a,con1}$, and circumferential cross-induced velocity, $W_{c,con1}$. Note that the rear disk does not induce circumferential induced velocity on the forward disk – only the wake swirls.
All of the above velocity components are taken into account in the contra-rotating blade-element scheme. It shows the forward and rear blade-elements which are described, each with its velocity diagram. The forward blade-element axial velocity components are the free stream, \( V \), the self-induced axial velocity, \( W_{a,1} \), and the axial cross-induced velocity which is induced by the rear disk, \( W_{a,1on2} \). Both axial induced velocities act to the same direction. The forward circumferential components are the tangential speed due to the blade rotating, \( \Omega \cdot r \), and the circumferential self-induced velocity, \( \frac{1}{2} W_{t,1} \). Note that the \( \frac{1}{2} \) factor on the self-induced circumferential velocity is due to the discontinuity of the swirl of the flow. Thus, the average \( \frac{1}{2} \) is taken for the blade-element velocity scheme.

The rear blade-element axial velocity components include the free stream velocity, \( V \), the axial self-induced velocity, \( W_{a,2} \), and the axial cross-induced velocity which the forward disk induces on the rear, \( W_{a,1on2} \). The circumferential velocity components include the tangential velocity, \( \Omega \cdot r \), the self-induced circumferential induced velocity, \( \frac{1}{2} W_{t,2} \), and the circumferential induced velocity which is induced from the forward on the rear disk, \( W_{t,1on2} \). The circumferential induced velocities are opposite direction. While the self-induced \( \frac{1}{2} W_{t,2} \) acts opposite of the propeller rotation, the cross-induced \( W_{t,1on2} \) acts in the same direction of the rear-propeller rotation. In this way, the wake of the rear propeller experience very low rotational velocity which is one of the main advantages of contra-rotating configuration. This also implies on the zero torque of the contra-rotating configuration.

![Figure 1](image-url) Contra-rotating propellers - schematic of the actuator disk and blade-element models depicting the velocity components.

Each of the disks in the contra-rotating problem is solved separately. Each solution influences the cross-induced velocities, \( W_{a,1on2} \) and \( W_{a,2on1} \). A convergence is reached after iterative process. The solution of the contra-rotating problem is based on the solution of an isolated single-rotating disk. First the forward disk is solved and the cross induced velocities \( W_{a,1on2} \) and \( W_{t,1on2} \) are estimated according to the relative position of the rear disk. Then the rear disk is solved and the cross-induced velocity \( W_{a,2on1} \) is calculated. The convergence of the disks solution is checked and then the convergence loop is closed. The convergence of the scheme is quick and generally after a few iterations both disks are converged.

The BEM for a single disk solution is the standard BEM as described in Ref. [2]. The main contribution of the current effort is the calculation of the cross-induced velocity components. This allows an existing single-rotating models to extend easily to a contra-rotating models. In what follows, the simplified model which uses to estimate the cross-induced velocities is presented.

### 3. Axial cross-induced velocities

The axial cross-induced velocities, \( W_{a,1on2} \) and \( W_{a,2on1} \) are calculated using the method described in Ref. [3]. It assumes that the axial induced velocity is constant in the radial coordinate. Although this assumption seems crude, it is quite accurate. Especially for optimized propellers, which are design to create radial uniform axial induced velocity. Using this assumption and simple momentum relation, the axial induced velocity, \( W_{a} \), along the axial coordinate, \( x \), can be found - Eq. (1).

\[
\frac{W_{a}(x)}{W_{a}} = 1 + \frac{x}{\sqrt{x^2 + R^2}}
\]

(1)

where \( W_{a} \) is the induced velocity on the disk plane, and \( W_{a}(x) \) is the induced velocity in the axial coordinate \( x \). \( R \) is the disk radius. Equation (1) suits negative \( x \) values, i.e. inflow, and positive \( x \) values – wake. For high values of
x, Eq. (1) result with the axial induced velocity in the far wake. It is twice the value of the disk’s (x=0) induce velocity. This match the classic result of the axial momentum theory presented by Glauert.[4]

Due to mass flux continuity, the inflow and wake radius, $R(x)$ can be found using Eq. (2).

$$ R(x) = \sqrt{\frac{V + W_a}{V + W_a(x)}} $$

(2)

$R(x)$ is the radius of the inflow or wake according to the sign of the axial coordinate, $x$.

The assumption of constant induced velocity along the radial coordinate enables using the simple momentum model which relates the axial induced velocity, $W_a$, and the produced thrust, $T$. [4]

$$ W_a = \frac{V}{2} + \sqrt{\left(\frac{V}{2}\right)^2 + \frac{T}{2\rho A}} $$

(3)

where $A$ is the disk area and $\rho$ is the air density.

Equation (3) can be written using the standard parameters of advance ratio, $J$, and thrust coefficient, $C_T$ as depicted in Eq. (4).

$$ W_a = \frac{J}{2} + \sqrt{\left(\frac{J}{2}\right)^2 + \frac{2C_T}{\pi}} $$

(4)

where $D$ is the disk diameter and $n$ is the rotational speed in revolutions-per-second, rps, units. Advance ratio, $J$, and the thrust coefficient, $C_T$, power coefficient, $C_p$, and propeller efficiency, $\eta$, are defined in Eq. (5).

$$ J = \frac{V}{nD}, \quad C_T = \frac{T}{\rho n^2 D^4}, \quad C_p = \frac{P}{\rho n^2 D^4}, \quad \eta = \frac{J \cdot C_T}{C_p} $$

(5)

where $P$ is the required power of the propeller.

4. Cross-induced circumferential induced velocity

The cross-induced circumferential induced velocity, $W_t$, model is based on simple momentum theory. Main assumption is that the wake is swirled at a constant rotation speed, $\Omega_t$. Again, similar to the constant radial axial induced velocity, the rigid wake geometry fits optimized propeller which most well designed propellers are fit to.

Thus, the circumferential induced velocity can be expressed using the wake rotational speed in Eq. (6).

$$ W_t = \Omega_t \cdot r $$

(6)

To find the wake rotational velocity, the actuator disk is divided to co-centric annuluses, each introduces a circumferential momentum, thus increase the circumferential by $W_t$. this enable to calculate the contribution of each annulus to the propeller torque, $dQ$, according to Eq.

$$ dQ = d\omega \cdot W_t \cdot r $$

(7)

where $d\omega$ is the mass flux through the annulus element. It is calculated using the axial velocity components through the disk plane – Eq. (8).

$$ d\omega = \rho \cdot (V + W_a) \cdot 2 \cdot \pi \cdot r \cdot dr $$

(8)

The total torque, $Q$, is calculated using the integration of Eq. (7) and substituting Eqs. (6) and (8).

$$ Q = \int_{\Omega_t}^{\infty} \rho \cdot (V + W_a) \cdot 2 \cdot \pi \cdot r \cdot dr \cdot \Omega_t \cdot r \cdot r $$

(9)

Assuming the axial induced velocity, $W_a$, is constant along the disk radii, the torque can be calculated and hence the wake rotational speed can be substantiated – Eq. (10).

$$ \Omega_t = \frac{2 \cdot Q}{\rho \cdot (V + W_a) \cdot 2 \cdot \pi \cdot R^4} $$

(10)

Using the power coefficient, $C_p$, definition in Eq. (5), and Eq. (4), the wake rotational speed can be presented in a non-dimensional manner – Eq. (11).

$$ \Omega_t = \frac{8}{\pi^2} J/2 + \sqrt{(J/2)^2 + 2C_T/\pi} $$

(11)

5. Biermann and Hartman test

Propeller performance maps for the Biermann and Hartman[5] test were calculated and are presented in Figure 2. It shows the performance maps for the 4-bladed propeller performance maps. Similar results, which are not presented here, were gained also for the 6 bladed propeller.

The maps are presented for 3 parameters; thrust coefficient, $C_T$, power coefficient, $C_p$, and propeller efficiency, $\eta$, as function of the advance ratio, $J$ - all are defined in Eqs. (5).

The Biermann and Hartman’s propellers shared the same blade geometry and had diameter of 120” (3.05 m). The blade uses Clark-Y airfoil family. The airfoil 2-D aerodynamic database was substantiated using MSES.[6]
Then PropSim (same tool used to generate the results in Ref. [1]) is used to calculate the propeller map. Figure 2 contains data for pitch angles vary from 20 degrees to 60 degrees for the 4 bladed propeller.

![Figure 2](image2.png)

**Figure 2.** Test and analysis comparison for Ref. [5], 4 blades, single rotating propeller performance

Up to pitch angle of about 50 deg, the curves comparison exhibits a good agreement, both for the numerical values and for the curve shape and trends. The comparison is considered mainly to the linear regime, i.e. to the higher values of the advance ratio for each pitch angle. For example, for pitch angle of 40 deg, the linear regime is for advance ratio of $J=1.5$ and above. For this regime, efficiency comparison shows a maximum difference of about 5% efficiency, which is considered good. The thrust and power coefficient slopes shows a very good agreement. For low advance ratio, comparison of blade-element method is due to fail, mainly due to the high angle of attack which the blade sections experience. This was discussed thoroughly in Ref. [7] and a correction model is incorporated to the sectional 2-D aerodynamic database, used for the blade-element model. Still, some irregularities appear for the very low advance ratio which is caused due to the stalled airfoil database, as created by MSES.

In most cases MSES over-predicts the maximum lift coefficient [8],[9] thus the thrust coefficient also being over-predicted. The power coefficient comparison, at the lower advance ratio regime is fair, i.e. drag is being well estimated by MSES.

For high pitch angles, above 50 degrees, the comparison is deteriorated. Again, this statement is relevant for the high advance ratio i.e. linear regime. The efficiency difference reaches up to 10%, although the slopes and general trends of the thrust and power coefficients remain accurate.

Figure 3 shows the results for the contra-rotating propeller configurations. Note that the pitch setting was not the same for the forward and rear propellers and the pitch as stated by Biermann and Hartman is presented on the performance charts.

Figure 3 shows the performance of the 4-bladed propeller, thus the front and rear disk has 2 blades each, and total of 4 blades, i.e. same disk solidity as the single rotating case. Similar results exist also for the 6 bladed propeller with 3 blades for the front propeller and 3 for the rear.

The results are given again for the three performance parameters; thrust coefficient, $C_T$, power coefficient, $C_P$, and propeller efficiency, $\eta$. The test results included separate measurement of the power coefficient for the forward and the rear propellers, thus an additional comparison is given for these measurements.

In general, the comparison of analyses to test results exhibits the same trends as for the single propeller comparison, presented in Figure 2. The thrust and power coefficients agrees well with the test results for pitch angles up to $\beta=50$ degrees. Above this pitch angle, the agreement deteriorates although the curve slopes and the pitch sensitivity exhibit good agreement. For low advance ratios, the agreement is poor due to the same reasons of the single-rotating case, as shown in Figure 2. The efficiency curves exhibit up to 5% difference for low pitch angles and this difference increase to about 10% for the high pitch angles. Again, the trends are accurate, thus the sensitivity to advance ratio and pitch angle show good agreement to the test results.

![Figure 3](image3.png)

**Figure 3.** Test and analysis comparison for Ref. [5], 4 blades, contra-rotating propeller performance
Comparing the separate propeller measurements of the power coefficient teaches that the rear propeller agreement is excellent while the forward is less accurate. This result is correct also for high pitch angles well beyond the threshold of $\beta=40$ degrees which was concluded from the thrust and power coefficient curves. Therefore, the separate power coefficient results might hold some insight to this phenomenon.

The explanation lays in Figure 1 which depicts the blade-element of the forward and rear propeller along their velocities components. According to this description, the front propeller uses only a single estimation for the cross-induced velocity, $W_{a,1on2}$, while the rear, more accurate propeller uses two separate estimations for the axial and circumferential cross-induced velocities, $W_{a,1on2}$ and $W_{t,1on2}$, respectively. It is expected that the rear propeller will exhibit worse comparison. It is influenced by both axial and circumferential cross-induced velocities; both are crudely estimated. On the contrary, it seems that these cross-induced velocities rather improve the model accuracy.

The circumferential induced velocity for the rear propeller contains two contradicting components. The self-induced circumferential induced velocity, $W_{t2}$, acts in the opposite direction from the cross-induced component, $W_{t,1on2}$. In a way, the wake of a contra-rotating propeller might consider as a non-swirled wake, which implicate on the zero-torque that a contra-rotating propeller produces. Thus, the rear propeller acts in a non-swirled environment, which the only circumferential velocity component is the tangential velocity due to its own rotational speed. This causes the blade-element scheme of the rear propeller to be much accurate than the forward propeller, which includes the self-induced circumferential velocity, $W_{t}$.

The accuracy of the circumferential induced velocity has major influence on the high pitch angles performance. Thus the forward propeller model is less accurate for high pitch angles. This explains the results of the separate power coefficient measurement and the exceptional agreement of the BEM model for the rear propeller. Moreover, it lays the blame for the disagreement of the total thrust and power coefficient, on the forward propeller, which acts similar to a single-rotating propeller and exhibits the same inaccuracies.

To further inspect the correctness of the proposed BEM for the contra-rotating case, a comparison for 5 pitch angles, $\beta=20$, 30, 40, 50, and 60 degrees, is presented in Figure 4. Here the curves are the same as in Figure 2 and Figure 3 for the 4-blade propeller. The purpose of Figure 4 is to present all curves; test, analysis, single rotating, SR, and contra-rotating, CR, on the same chart. Thus, the trends can be examined using the differences between the various curves. As stated above, very similar results were gained for the 6 bladed propeller.

The main trend is the difference between the SR and the CR. SR curves are solid while the CR curves are dashed. One can examine the gap between the solid and dashed curves for both test results (in red) and analyses (in blue). Both thrust and power coefficient exhibit decrease in value transferring from SR to CR. On the same time, the efficiency increases. As the pitch angle increases these trends become more prominent. The above observations repeat in the same manner for both the test and analysis results, thus proves the correctness of the current BEM model for the contra-rotating case.

6. Harrington test

Further validation of the contra-rotating model is given for hovering rotor, using the results by Harrington.[10] Harrington’s test involves 2 different, hovering, 2-bladed, 25 ft (7.6 m) diameter rotors in a single-rotating configuration (SR) and contra-rotating, i.e. total of 4 blades. The rotor blades use NACA-4-digits symmetrical airfoils. The 2-D aerodynamic database, which was used to analyze these rotors, was substantiated from Ref. [11].

Figure 5 and shows the comparison of the rotor thrust and power coefficients, $C_{T,rotor}$, $C_{P,rotor}$, respectively, as defined in Eq. (12)

$$C_{T,rotor} = \frac{T}{\rho A V_{tip}^2} \quad C_{P,rotor} = \frac{P}{\rho A V_{tip}^2}$$

(12)

where $A$ is the disk area and $V_{tip}$ is the blade’s tip velocity.

These coefficients are equivalent to the propeller thrust and power coefficient, $C_T$ and $C_P$, respectively as defined in Eqs. (5). Thus, the various coefficients can be related according to Eq. (13).
Figure 5 and Figure 6 show the rotors Figure-of-Merit, FM, which is the measure of the rotor quality at hover and is defined in Eq. (14).

$$FM = \frac{C_{T,rotor}}{\sqrt{C_{P,rotor}^{1.5}}}$$

(14)

The results show good comparison between the experiment and analyses, both for the power to thrust coefficient ($C_{P,rotor}$ vs. $C_{T,rotor}$) chart and for the Figure-of-Merit to thrust coefficient ($FM$ vs. $C_{T,rotor}$) chart. The trends are similar and values are close within 5% error. The charts include both the SR and CR configuration. The difference between the two configurations is very similar comparing the test results and analysis. The power coefficient is increased by a constant value comparing the SR and CR configuration, while the curve slope and trend remains the same. As expected, the FM curves exhibit decrease from SR to CR configuration due to the increase of the required power for the same thrust coefficient – as depicted in Eq. (14). Similar to Biermann and Hartman test in Figure 4, the contra rotating model predicts well the differences between the SR and CR configuration, thus it is again validated.

7. Dunham et-al test

A third source for contra-rotating test results is available in Dunham et-al experiment.[12] They used the SR-2 propeller blades in a single-rotating 8 bladed propeller and 4+4 blades, contra-rotating configuration in a low-speed wind-tunnel, although the SR-2 was designed for high Mach numbers.[13] As a transonic propeller it has exceptionally thin blades, down to 2% thickness ratio, with relatively high chord to radius ratios. Sand et-al [14] contains a detailed model for the NACA-16 airfoil series, but it is not applicable for such thicknesses. Thus, a 2-D CFD analyses were conducted to substantiate the 2-D aerodynamic database, i.e. using EZair RANS code,[15] which was validated extensively.[16] As stated above, for the current case, the cross sectional Mach number is low due to the test conditions of this experiment.

The Dunham et-al experiment measured the performance of 3 pitch angles for the single-rotating, SR, configuration ($\beta=30.45, 40.30, 50.15$ degrees) and a single pitch angle ($\beta=41.30$ degrees) for the contra-rotating configuration. Unfortunately, for the CR configuration, only the thrust coefficient, $C_t$, was measured, thus somewhat lean comparison is available. Nevertheless, Figure 6 presents the thrust coefficient, $C_t$, power coefficient, $C_p$, and efficiency, $\eta$, of the SR-2 propeller. The SR results exhibit good agreement for low pitch angle $\beta=30.45$ deg. As the pitch angle increases the agreement deteriorates - for the high pitch angle of $\beta=50.15$ degrees, the agreement is poor. For the intermediate pitch angle, $\beta=40.3$, the agreement is fair, with efficiency difference of up to 5%. The CR result seems to be good, although it is relevant only to the thrust coefficient. Still, the level of accuracy of the SR and CR results is similar, which again proves the correctness of the suggested contra-rotating analysis.
Figure 6. Test and analysis comparison for Ref. [12], SR & CR comparison

8. Summary and Conclusions
The paper exhibits the extension of a single-rotating blade-element model to case of contra-rotating configuration. While the single-rotating blade-element model is very common, the contra-rotating model is somewhat more complicated. The importance of the current paper is enabling the use of legacy single-rotating models and by simple means, extending it to contra-rotating applications. Moreover, the new contra-rotating model share the same accuracy level with the former, single rotating model.

The extension is based on iterative scheme of the single-rotating model with additional induced velocity components, which are induced by one rotating disk on the other. These cross-induced velocities are estimated using simple axial momentum theory for the axial cross-induced velocities and simple rigid rotated wake for the cross-induced circumferential velocity, which the forward rotating-wing induced on the rear.

Using this simple model, a well validated blade-element tool was used for the analyses of contra-rotating wing configurations. Three different test results were used as validation cases. These experiments contain propeller, hovering rotor, and prop-fan configurations. All tests used a single-rotating and dual-rotating configuration of the same blades, thus a focused view on the difference between the single and contra-rotating configuration can be conducted.

Each validation case was comprised of both single- and contra-rotating cases. The contra-rotating validations exhibit the same agreements and inaccuracies which were resulted for the single-rotated cases. Moreover, the differences between the single and dual-rotating configuration was replicated accurately by the analyses, and match in a good manner to the same difference exhibited by the experiment results. This proved the applicability of the contra-rotating extension model which used the same blade-element model for the single-rotating cases.

In general, for linear regime, non-stalled blades, the agreement is very good, up to 5% efficiency or figure-of-merit (for hovering cases). The deterioration of the agreement happens for the low advance ratio regime or high pitch cases. These inaccuracies are common both to the single and dual-rotating configuration, thus it is an inherit inaccuracy of the blade-element model.

In one of the validation cases, the forward and rear rotating-wings were measured separately. This enables to learn that the forward disk analysis suffers from inaccuracies more than the rear disk. The latter shows very good agreement between analyses and test data. This is due to the low circumferential induced velocity which the rear disk experiences. The circumferential induced model is one of the reasons for inaccuracies of single-rotating blade-element model, for high pitch cases. Thus, the rear rotating-wing does not suffer from this issue.

9. References

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