APPLICATION OF PREDICTIVE CONTROL TECHNIQUES TO A HELICOPTER BLADE SAILING SYSTEM

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Abstract: This paper concerns the application of a Model Predictive Control (MPC) technique to a helicopter blade-sailing system in the presence of unsteady flow effects. The aeroservoelastic analysis focuses on the performance of a proposed individual blade-root controller (IBRC) with respect to the reduction of blade flapping vibrations in articulated rotors during engagement shipboard operations. The simulation results show that the proposed aeroelastic controller can yield tunnel-strike suppression without actuator saturation.

Keywords: Predictive control, Helicopter blade sailing, aeroservoelasticity.

1. INTRODUCTION

Flow-induced unsteady loads are often related to large vibrations and damage in flexible structures. Shipboard helicopters, operating in the hostile maritime environment from frigate-like platforms, are especially susceptible to these effects during rotor engagement/disengagement operations under high wind-over-deck (WOD) conditions. These dangerous conditions are amplified by the ship structure, which generates flow velocity gradients and vortices over the flight deck. Therefore, shipboard helicopter operations are among the most hazardous military operations and the shipboard environment imposes severe restrictions on the missions and determines stringent requirements for the design of aerial vehicles.

The problem of flight in the vicinity of ships is usually called Dynamic Interface (DI) problem [1]. Among the dynamic phenomena in the DI that must be analyzed and controlled, one is especially important for rotary-wing aircraft: blade sailing.

Blade sailing is an aeroelastic transient phenomenon characterized by the occurrence of large flapping vibrations, possibly associated with tunnel/tail-boom strikes, due to fluid-structure interactions during engagement or disengagement operations of helicopter rotors under high wind conditions [2].

The blade-sailing control problem has a theoretical importance, due to the nonlinear time-varying characteristics of the associated blade flapping oscillator, which is also subjected to large disturbances.

Considering the ubiquitous use of the shipboard helicopter in critical defense missions, the problem has a practical relevance as well, as shown by a recent NATO symposium about the study of flow-induced unsteady loads and the impact on military applications [3].

Model predictive control (MPC) refers to a class of computer control algorithms that utilize an explicit process model to predict the future response of a plant. At each control interval an MPC algorithm attempts to optimize future plant behavior by computing a sequence of future manipulated variable adjustments. The first input in the optimal sequence is then sent into the plant, and the entire calculation is repeated at subsequent control intervals. Originally developed to meet the specialized control needs of power plants and petroleum refineries, MPC technology can now be found in a wide variety of application areas including chemicals, food processing, automotive, and aerospace applications[4].

In this sense, it may be interesting to consider the potential use of model predictive control (MPC) strategies for helicopter-blade sailing system. In fact, the ability of handling operational constraints in an explicit manner is one of the main reasons for the popularity of predictive controllers in industrial applications [5-6].

The obtained theoretical results may be helpful for better understanding of helicopter blade-sailing alleviation using the application of a predictive control methodology considering several fluctuation flow conditions.

2. AEROELASTIC MODELING

For control purposes the blade-sailing aeroelastic model can be greatly simplified by considering the forces and moments acting only in the flapping plane.

Fig. 1 shows the forces at a blade element for the simplified blade-sailing planar model, according to a frame rotating with the blade. The simplified diagram of forces at a planar blade element [7] in Fig. 1 illustrates the main factors that govern the blade-sailing behavior.

Ship motion effects are not included. The resulting moments about the flapping hinge in conjunction with the droop/flap stop effects, modeled as a nonlinear rotational
spring, determine the blade tip deflections related to the angle $\beta$.

![Figure 1: Forces at a flapping planar blade element for the proposed blade-sailing model (rotating frame)](image)

Fig. 2 shows the flow velocity components in the plane of the rotor for the proposed blade-sailing model, considering the WOD conditions. $V_{WOD}$ and $\Psi_{WOD}$ are, respectively, the magnitude and direction of the incoming wind velocity with respect to the ship centerline.

![Figure 2: Flow velocity components for the WOD conditions](image)

The blade-sailing modeling is based on a proposed rotary-wing aerelastic scheme to articulated shipboard rotor blades, according to the Figs. 1 and 2, taking into account some simplifying assumptions [8-10].

Generally, the flow field that affects the rotor behavior is non-uniform and unsteady, thus, the three velocity components vary with space and time. Mean flow velocity gradients arise due to the ship geometry and the fluctuating flow velocity components arise due to the ship geometry and also to the meteorological effects, like turbulence from storms.

To simplify the aeroelastic analysis, only the lateral (90° or 270°) wind condition is considered, focusing the ship airwake modeling on the effects of the horizontal and vertical velocity components related to this worst-case blade-sailing condition [2]. The WOD velocity component $V_y$ is neglected. For a typical frigate-like configuration with only one flight deck, as considered in this work, the WOD horizontal velocity $V_x$ for the lateral condition can be considered uniform along the shipboard rotor.

The mean flow vertical velocity related to the interaction between the lateral undisturbed wind flow and a typical frigate-like structure can be approximated by a linear distribution along the flight deck and the helicopter rotor [11-12]. Therefore, for a rotor blade element at radial station $r$ and azimuth $\Psi$, and constant WOD horizontal velocity component $V_x$, the WOD mean vertical velocity, according to the linear distribution approximation ("linear gust model"), is given by:

$$\overline{V}_y = K_v V_x \frac{r}{R} \sin \Psi .$$

(1)

Unsteady flow effects can be modeled by considering a sinusoidal gust across the rotor disk for the WOD fluctuating vertical velocity component, representing the effects of the dominant frequency $\omega_f$ of the ship airwake on the helicopter rotor, as follows:

$$V_y' = K_v V_x \sin \omega_f t .$$

(2)

The gust amplitude parameters $K_v$ and $K_r$, and the sinusoidal gust frequency $\omega_f$ govern the flow-induced unsteady loads associated with the WOD vertical velocity component, which characterizes a flow field over the flight deck that varies with space and time according to Eqs. 1 and 2. The aerodynamic components affecting a shipboard rotor blade can be calculated according to the blade-element theory, as follows [13]:

$$V_x = V_{WOD} \cos \Psi_{WOD}, \quad V_y = V_{WOD} \sin \Psi_{WOD}, \quad U_{f} = r \Omega - V_x \cos \Psi + V_y \sin \Psi, \quad U_T = r \Omega + (V_x \sin \Psi + V_y \cos \Psi) \beta - V_x .$$

(3)

In particular, $\Psi_{WOD}$ is equal to 90° for lateral port side winds and to 270° for lateral starboard side winds. $U_P$ and $U_T$ are, respectively, the normal and tangential flow velocity components at the blade element at radial station $r$, azimuth $\Psi$ and flapping angle $\beta$.

As mentioned in the introduction, previous researches on blade-sailing active control were based on swashplate actuation [13], trailing edge flaps [14], active twist [15] and individual blade root control [8-10].

Then, the governing equation of motion of the blade-sailing helicopter is given by:

$$\beta + \frac{\Omega^2}{8} [1 + \frac{4}{2} (\mu_x \sin \Psi - \mu_y \cos \Psi) - \frac{1}{2} \sin^2 \Psi + 2 \mu_y \cos \Psi] \beta + \frac{\Omega^2}{8} [1 - \frac{4}{2} (\mu_x \sin \Psi - \mu_y \cos \Psi) - \frac{1}{2} \sin^2 \Psi + 2 \mu_y \cos \Psi] \beta + \frac{\Omega^2}{8} [1 + \frac{4}{2} (\mu_x \sin \Psi - \mu_y \cos \Psi) - \frac{1}{2} \sin^2 \Psi + 2 \mu_y \cos \Psi] \beta + \frac{\Omega^2}{8} [1 + \frac{4}{2} (\mu_x \sin \Psi - \mu_y \cos \Psi) - \frac{1}{2} \sin^2 \Psi + 2 \mu_y \cos \Psi] \beta + \frac{\Omega^2}{8} [1 + \frac{4}{2} (\mu_x \sin \Psi - \mu_y \cos \Psi) - \frac{1}{2} \sin^2 \Psi + 2 \mu_y \cos \Psi] \beta + \frac{\Omega^2}{8} [1 + \frac{4}{2} (\mu_x \sin \Psi - \mu_y \cos \Psi) - \frac{1}{2} \sin^2 \Psi + 2 \mu_y \cos \Psi] \beta .$$

(4)

Therefore, according Eq. (4), the single-degree-of-freedom blade-sailing behavior is governed by a nonlinear ordinary differential equation (ODE) with time-varying coefficients. To carry out the parametric and control analysis, the nonlinear aeroelastic blade-sailing Eq. (4) can be rewrite as following:
\[ \dot{\beta} + c_\beta(t)\dot{\beta} + c_\beta(t)\beta + \sigma(\beta) = u(t) + x_\sigma(t) \quad (5) \]

where \( c_\beta(t) \), \( c_\beta(t) \) are the damping and stiffness time varying coefficients, respectively, \( \sigma(\beta) \) is the nonlinear stiffness function related to the droop/flap stop effects, \( u(t) \) represent the active control input and \( x_\sigma(t) \) is the sum of the effects due to the exogenous inputs at the right side of Eq. (4).

Considering \( x = \begin{bmatrix} \beta \\ \dot{\beta} \end{bmatrix} = [x_1, x_2]^T \), a state-space model can be obtained from Eq. (5) as follows:

\begin{align*}
\dot{x}_1 &= x_2 \\
\dot{x}_2 &= -c_\beta(t)x_2 - c_\beta(t)x_1 - \sigma(x_1) + u(t) + x_\sigma(t)
\end{align*} \quad (6)

3. MODEL VERIFICATION

The verification of the blade-sailing model given by Eq. 7 is obtained by comparison with results given in [13] and [11] from models validated with experimental data, according to a fourth-fifth order Runge-Kutta numerical simulation. Table 1 show the parameter values for the simulations, which are based on the H-46 Sea Knight shipboard helicopter characteristics, in conjunction with a WOD linear gust parameter \( K_v \) equal to 0.25.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>( \gamma ) (Lock number)</td>
<td>7.96</td>
</tr>
<tr>
<td>( \Omega_0 ) (nominal rotor rotational speed)</td>
<td>27.65 rad/s</td>
</tr>
<tr>
<td>( V_y ) (lateral WOD velocity)</td>
<td>-42.5 kt</td>
</tr>
<tr>
<td>( V_x ) (longitudinal WOD velocity)</td>
<td>0 kt</td>
</tr>
<tr>
<td>( R ) (rotor radius)</td>
<td>25.5 ft</td>
</tr>
<tr>
<td>( \omega_{nr} ) (blade non-rotating flapping frequency)</td>
<td>6 rad/s</td>
</tr>
<tr>
<td>( \beta_{ds} ) (droop stop angle)</td>
<td>-1°</td>
</tr>
<tr>
<td>( \theta_{fl} ) (flap stop angle)</td>
<td>1°</td>
</tr>
<tr>
<td>( \theta_{cp} ) (collective pitch angle)</td>
<td>3°</td>
</tr>
<tr>
<td>( \theta_{bt} ) (built-in twist angle)</td>
<td>-8.5°</td>
</tr>
<tr>
<td>( \theta_{lc} ) (longitudinal cyclic angle)</td>
<td>2.5°</td>
</tr>
<tr>
<td>( \theta_{lc} ) (lateral cyclic angle)</td>
<td>0.0693°</td>
</tr>
</tbody>
</table>

A numerical simulation is carried out for the linear gust model of the ship airwake and Fig. 3 illustrates the results.

Figure 3: Flapping response for the linear gust model

The flapping response diagram in Fig. 1 shows a very good agreement of the proposed model with the results given in [11]. The time range 0-4 seconds of the simulation corresponds to 6-10 seconds for the actual H-46 engagement behavior, when rotor rotational speeds are low, varying from 10% to 46% of the nominal rotation speed (NR).

4. PREDICTIVE CONTROL STRATEGY

Figure 4 presents the main elements of the discrete-time predictive control formulation adopted in this work. The process model is employed to calculate output predictions up to \( N \) steps in the future, where \( N \) is termed “Prediction Horizon”. Such predictions are determined on the basis of all information available up to the present time (kth sampling instant), and are also dependent on the control sequence that will be applied. The optimization algorithm is aimed at determining the sequence \( \{u[k-1+i], i=1,...,M\} \) that minimizes the cost function specified for the problem, subject to constraints on the input and output of the plant. The value of \( M \) (“Control Horizon”) is smaller than \( N \), and the optimization assumes that \( u[k-1+i] = u[k+M-1] \) for \( M < i \leq N \). The control is implemented in a receding horizon manner, that is, only the first element of the optimized control sequence is applied to the plant and the optimization is repeated at the next sampling instant, on the basis of fresh state measurements.
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Figure 4: Predictive control loop employing state feedback. The plant input, the output of interest and the reference signal are denoted by \( u \in \mathbb{R}^2 \), \( y \in \mathbb{R} \), and \( r \in \mathbb{R} \), respectively. In addition, \( \hat{y}_{k+i} \) denotes the prediction of the output at instant \( k+i \) on the basis of the measured state \( x_k \in \mathbb{R}^2 \). The optimal control at instant \( k \) is denoted by \( u^* \).

The task of designing a predictive control law can be divided into two phases. The first concerns the identification of a mathematical model capable of representing the behavior of the real process. The second is linked to implementation of a predictive control algorithm appropriate. This determines the control actions based on the minimization of a cost function considering future responses predicted in the model of the process.

Assuming the linear model for the plant dynamics, the state equations and output can be discretized in the form below:

\[
x_{k+1} = Ax_k + Bu_k \\
y_k = Cx_k
\]

(7)

It can then be shown [5] that the output predictions can be related to the future control variations as \( \hat{Y} = G\Delta U + \hat{F} \), where

\[
G = HT_N, \quad \hat{F} = H_{1N}u_{k-1} + \Phi x_k
\]

and

\[
\hat{Y} = \begin{bmatrix} \hat{y}_{k+1} \\ \hat{y}_{k+2} \\ \vdots \\ \hat{y}_{k+N} \end{bmatrix}, \quad H = \begin{bmatrix} CB & 0 & \ldots & 0 \\ C^2B & CB & \ldots & 0 \\ \vdots & \vdots & \ddots & \vdots \\ C^{N-1}B & C^{N-2}B & \ldots & CB \end{bmatrix}, \quad \Phi = \begin{bmatrix} CA \\ CA^2 \\ \vdots \\ CA^N \end{bmatrix}
\]

where \( T_M \) is a lower triangular matrix of ones (\( T_M(i,j) = 1 \) for \( i \geq j \) and zero otherwise). Thus, the quadratic cost function that penalizes both tracking errors and control variations can be written in matrix form as;

\[
J(\Delta U) = (Y - R)^T (Y - R) + \rho \Delta U^T \Delta U
\]

(8)

where

\[
R = e[k+1], e[k+2], \ldots, e[k+N]
\]

\[
\Delta u[k] = u[k] - u[k-1]
\]

\[
\Delta U = \Delta u[k+1], \Delta u[k+2], \ldots, \Delta u[k + M - 1]
\]

is the vector of optimization variables. It can thus be seen that the cost is a quadratic function of the optimization variables. The design parameter \( \rho > 0 \) may be adjusted to achieve a compromise between minimizing the output tracking error and minimizing variations on the control signal. Decreasing \( \rho \) tends to increase the speed of the closed-loop response at the cost of a larger control effort and a greater sensitivity to measurement noise.

In the absence of constraints, the control sequence \( \Delta U^* \) that minimizes the cost function is given by

\[
\Delta U^* = (G^TG + \rho I_M)^{-1} G^T (R - \hat{F})
\]

where \( I_M \) is an \( M \times M \) identity matrix.

If restrictions on the manipulated and controlled variables of the form \( \Delta u_{min} \leq \Delta u[k+i] \leq \Delta u_{max} \), \( i = 1, \ldots, M \), \( u_{min} \leq u[k+i] \leq u_{max} \), \( i = 1, \ldots, M \), \( y_{min} \leq \hat{y}[k+i] \leq y_{max} \), \( i = 1, \ldots, N \) are to be satisfied, the minimization of the cost is subject to the following linear constraints on \( \Delta U \):

\[
\begin{bmatrix} I_M \\ -I_M \\ T_M \\ -T_M \\ G \end{bmatrix} \Delta U \leq \begin{bmatrix} \Gamma_M \Delta u_{max} \\ -\Gamma_M \Delta u_{max} \\ \Gamma_M [u_{max} - u(k-1)] \\ \Gamma_M [u(k-1) - u_{min}] \\ \Gamma_M y_{max} - \hat{F} \\ \hat{F} - \Gamma_M y_{max} \end{bmatrix}
\]

(9)

where \( T_M \) is a lower triangular matrix of ones (\( T_M(i,j) = 1 \) for \( i \geq j \) and zero otherwise) and \( \Gamma_M \), \( \Gamma_N \) are \( M \times 1 \) and \( N \times 1 \) column vectors of ones, respectively [4]. In this case, the unconstrained solution may not be a feasible point. The optimization problem then becomes one of Quadratic Programming [5].

In this study, the nonlinear equation of blade sailing, presented in the previous section, will be used in the simulations of the model. First, the model predicted was linearized around an equilibrium position. The system below represents the linearized equations of the model in question:

\[
\dot{x} = -\Omega^2 (1 + \frac{0.52}{\Omega^2}) x + \frac{0.52}{\Omega^2} u(t)
\]

(10)
Assuming that a zero-order-hold will keep the control signal constant between sampling instants, the model matrices resulting after linearization and discretization are as follows:

\[
A_d = \begin{bmatrix}
1 & 0.001 \\
0.0634 & 0.9948
\end{bmatrix},
B_d = \begin{bmatrix}
0 \\
0.0274
\end{bmatrix},
C_d = [1 
0]
\]

All simulations were carried out by using the Matlab software in the Simulink environment. A specific Matlab S-function was written to implement the predictive control law. The Quadratic Programming problem was solved by using the quadprog function of the Matlab Optimization Toolbox.

In order to investigate the effect of varying the prediction horizon \( N \), the cost parameter was fixed at \( \rho = 0.00001 \), the control horizon was fixed at \( M = 5 \) and four values of \( N \) were tested (\( N = 5, 10, 30 \)). Figure 5 presents the resulting responses for linear helicopter blade sailing system. On the basis of the results presented in Fig. 5, it was deemed that \( N = 10 \) provides a good compromise between speed of response and damping. Therefore, such a value was adopted for the prediction horizon.

As mentioned, they superimpose the blade collective and cyclic commands, according to the IBRC scheme. Therefore, Fig. 6 shows the simulation results considering the system with and without the action of the predictive controller. Note, that when considering the control system response has a smaller overshoot, while improving the settling time of response, thus the system shows a less oscillatory. In this case we used as parameters, \( \rho = 0.00001 \), \( N = 10 \) and \( M = 5 \).
Now, the next simulations illustrates the case when occur the fluctuation flow condition in relation to nominal value of $V_{WDD}$. Figs. 9 to 14 show the fluctuation flow corresponds to 5%, 10% and 15% respectively of the nominal value. Note that in all situation the system present a blade sailing phenomena but when is introduced the MPC a significant reduction of flapping vibration is observed in all cases.
Simulation results show that the proposed MPC IBRC-based controller, which was designed for a nominal steady flow tunnel-strike condition, has a good performance under typical unsteady flow conditions as well, avoiding tunnel-strike occurrences and yielding significant reduction in upward flapping deflections without actuator saturation.

5. CONCLUSION

The results obtained in this case study suggest that MPC methodologies may be a promising alternative to the control of helicopter blade sailing system. As regards the real-time deployment of the MPC controller, the use of a linear prediction model, as the one adopted in this work, may be advisable. In fact, the quadratic programming problem stemming from the combination of a linear model, a quadratic cost function, and linear constraints, can be solved by very efficient numerical algorithms [5]. The simulations showed that a MPC technique can compensate high lift conditions during engagement shipboard operations by modifying the response of the flapping oscillator. The results showed that a MPC device can significantly reduce the blade-sailing vibrations, avoiding tunnel-strike occurrences at severe unsteady flow conditions and for several fluctuation flow conditions. The proposed MPC-IBRC scheme yields tunnel strike suppression and significant blade-sailing reduction in upward blade tip deflections without actuator saturation (limits of ±6°).

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REFERENCES


