ACTIVE CONTROL OF HELICOPTER ROTOR BLADES WITH INDUCED STRAIN ACTUATORS

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Abstract

Rotor blade vibration reduction based on higher harmonic control - individual blade control (HHC-IBC) principles is presented as a possible area of application of induced strain actuators (ISA). Recent theoretical and experimental work on achieving HHC-IBC through conventional and ISA means is reviewed. Although the force-displacement and power-energy estimates vary significantly, some common-base values are identified. Hence, a benchmark specification for a tentative HHC-IBC device based on the aerodynamic servo-flap principle using ISA is developed. Values for the invariant quantities of energy, power, and force-displacement product are identified, along with actuator displacement and force values of practical interest. The implementation feasibility of this specification into an actual ISA device is then discussed. It is shown that direct actuation is not feasible due to the large required length of the ISA device, resulting in excessive compressibility effects (displacement loss and parasitic strain energy). Indirect actuation through a displacement amplifier was found to be more feasible, since this arrangement allows the matching of internal and external stiffness. A closed form formula was developed for finding the optimal amplification gain for each required value of the closed-loop amplification ratio. Preliminary studies based on force, stroke, energy and output power requirements show that available ISA stacks coupled with an optimally designed displacement amplifier might meet the benchmark specifications.

1 Introduction

1.1 Rotor Blade Active Vibration Control through HHC and IBC

A traditional way of controlling the pitch feathering motion of helicopter rotor blades is the swash-plate mechanism. This low-frequency device is designed for collective (quasi-steady) and cyclic (1/rev) control of the pitch motion \( \theta(t) = \theta_0 - a \cos \Omega t - b \sin \Omega t \), where \( \Omega \) is the rotor angular speed. Higher harmonic control (HHC) superposes an oscillatory modulation of frequency \( \omega \) on the basic pitch control of frequency \( \Omega \):

\[
\theta(t) \sin \omega t = (\theta_0 - a \cos \omega t - b \sin \omega t) \sin \omega t
\]

\[
= \theta_0 \sin \omega t - \frac{a}{2} [\sin(\omega - \Omega)t + \sin(\omega + \Omega)t]
\]

\[
- \frac{b}{2} [\cos(\omega - \Omega)t + \cos(\omega + \Omega)t]
\]

Since helicopter vibrations appear at multiples of rotor speed, HHC frequencies of the form \( \omega = \Omega n \) are usually used, thus resulting in pitch modulations not only at \( \Omega \) but also at \( (N-1)\Omega \) and \( (N+1)\Omega \). Extensive theoretical and experimental studies have shown that HHC can be an effective means of rotor blade vibration control, and reductions as large as 90% have been reported. However, two major issues limit the usefulness of conventional HHC: (a) the swash plate is an inherently low-frequency device and hence acts as a low-pass filter on the HHC input; and (b) HHC affects all the blades equally, while real rotor blades may differ noticeably in their detailed aerodynamic and inertial characteristics.

Individual Blade Control (IBC) denotes a method by which the blades of a rotor system have their pitch motion controlled individually. Thus, many additional issues may be addressed with this control concept, such as: blade tracking, and lift improvement through a 2/rev modulation. As a means of vibration control, the IBC implementation of the HHC concept can address the vibrations due to manufacturing variability between the blades, and also is not restricted by the \( N+1 \), \( N-1 \) phenomenon specific to swash plate control. An effective HHC-IBC device should also have a good frequency response characteristic, and thus produce the least possible filtering of the HHC signal.

Various engineering options are possible for implementing the HHC-IBC concept. One is to transform the conventional push-rods, connecting the swash-plate with the blade root, into active devices, e.g., hydraulic actuators. However, significant values of the aerodynamic pitch moment (450 - 780 Nm) must be overcome. A more efficient way is to utilize the principle of servo-aerodynamic

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that takes energy from the airstream to modify the rotor blade pitch. One application of this principle is the servo-flap concept. A limited span, trailing edge flap is used to produce aerodynamic pitching moments that in turn modify the pitch setting of the flexibly-restrained blade. The Kaman SH-2G Seasprite helicopter uses a mechanically operated servo-flap for primary (collective and cyclic) rotor blade control. However, the SH-2G servo-flaps are operated by a traditional swash-plate through push rods inside the blade and are not suited for HHC-IBC. Efficient vibration control requires frequency characteristics higher than can be offered by such mechanical systems. Remote electrical operation is an attractive alternative, and solid state ISA devices present numerous opportunities in this direction.

1.2 Induced Strain Actuation Principles

In certain materials, electro-mechanical coupling occurs in the form of an electrical charge being generated by an externally applied stress and, vice-versa, a strain being generated by an applied electric field. For active control applications, the interest is directed towards materials displaying the latter effect, i.e., the induced strain actuation (ISA) effect. Such ISA materials can respond either linearly (e.g., piezoelectric) or quadratically (e.g., electrostrictive) to the applied electric field. An associate behavior is displayed by the magnetostrictive materials where an induced strain is produced by an applied magnetic field. The general constitutive equations for an ISA material are

\[ S_{ij} = s_{ijkl} T_{kl} + d_{ijkl} E_k E_l + M_{ijkl} E_k E_l, \]

\[ D_i = d_{ijkl} T_{kl} + e_{ikl} E_k, \]  

where \( S_{ij} \) is the strain, \( T_{kl} \) the stress, \( E_k \) (or \( E_l \)) the electric field, \( s_{ijkl} \) the compliance, \( d_{ijkl} \) the linear ISA coefficients, and \( M_{ijkl} \) the quadratic ISA coefficients. For piezoelectric materials, the linear coefficients \( d_{ijkl} \) are dominant, whereas for an electrostrictive material, the quadratic terms \( M_{ijkl} \) are dominant. Magnetostrictive materials have a behavior similar to that of electrostrictive materials (i.e., dominantly quadratic). Widely developed in the last decade, ISA materials have shown remarkable performance, and positive induced strains in excess of 0.075% have been reported. The variation of induced strain with electric (or magnetic) field is comparatively presented in Figure 1 for commercially available ISA materials: PZT (a piezoelectric ceramic); PMN (an electrostrictive ceramic); and TERFENOL (a magnetostrictive alloy consisting of iron and two rare earth elements). The maximum strain is about 0.075% for PZT, 0.075% for PMN, and 0.160% for TERFENOL. (Higher values have also been occasionally reported.) Due to their quadratic characteristic, PMN and TERFENOL materials can only produce positive (i.e., expansion) strain, and hence compressive loads. The linear characteristic of the PZT material suggests a tension-compression capability, but its strength under tension loading is much less than under compression. Hence, it is reasonable to say that direct ISA applications are limited to compression loads. For bilateral, tension-compression, cycles, either a biased (offset) operation, or a counter-acting actuator pair, are normally used.

![Figure 1 Induced strain vs applied field for several ISA materials](image-url)

Though equation (1.1) represents a tensorially fully coupled behavior, most actuator applications of ISA materials make use of the ISA effect in the polarization \( (\Omega_x) \) direction only. A common example is that of an ISA stack (resembling the early voltaic piles of the last century) in which thin washers of the ISA material are intercalated with metallic electrodes alternatively charged. Thus, a high electric field can be applied throughout the ISA material, ensuring good performance. Commercially available ISA stacks of typically 150 mm length display free ISA displacement \( (x_{ISA}) \) in excess of 0.100 mm. However, due to internal compressibility, only a portion of the ISA displacement can be delivered externally. Denoting the quasi-static internal stiffness by \( k_i \), one computes the compressibility loss as \( x_i = F/k_i \), and hence the externally delivered displacement is \( x_e = x_{ISA} - x_i \). The higher the external load, the lower the externally deliverable displacement. For the fully constrained case, the actuator is blocked and all the ISA displacement is consumed internally. Since work is the product of force and displacement, it is apparent that the work done externally by an ISA actuator is zero both at zero load and at stalling load. Its variation is parabolic, and a maximum value is attained when the internal and external components of the ISA displacement are equal. A similar conclusion is obtained if we reason in terms of energy. Assuming a quasi-steady external stiffness \( k_e \), one writes the internal and external strain energies, and the corresponding ISA energy, as:
\[ E_i = \frac{1}{2} k_i x_i^2, \quad E_e = \frac{1}{2} k_e x_e^2, \]
\[ E_{EA} = E_i + E_e = \frac{1}{2} k_i x_i^2 + \frac{1}{2} k_e x_e^2. \]  

(1.2)

Liang, Sun and Rogers, performed a comprehensive analysis of energy and power aspects using an electromechanically coupled model. Plots of \( E_i, E_e, \) and \( E_{EA} \) against the external stiffness parameter \( r = k_e / k_i \) are given in Figure 2. It can be seen that while the parasitic internal energy steadily increases, the externally delivered energy reaches a maximum, after which it starts to decrease.

![Figure 2](image)

Figure 2  Variation of internal, external, and ISA energies with stiffness ratio \( r \). Maximum external energy is attained at \( r = 1 \).

Hence an optimum condition exists (\( r = 1 \), i.e., \( k_i = k_e \)) for which the delivered energy, and hence the energy per unit volume, and per unit weight, are maximum. Typical values (Table 1) are 4.078 kJ/m\(^3\) and 0.666 J/kg, respectively. Another aspect to be addressed is that of energy transmission efficiency, i.e., what percentage of the ISA energy gets delivered externally. Since part of the energy is stored internally due to compressibility, we define the energy transmission efficiency as

\[ \eta_{energy}(r) = \frac{E_e(r)}{E_{EA}(r)} \]  

(1.3)

Figure 2 shows that a given external energy demand can be satisfied by two different values of the stiffness ratio, \( r_1 \) and \( r_2 \). The lower value, \( r_1 \), yields a better energy transmission efficiency, since \( \eta(r_1) > \eta(r_2) \). Hence, it can be concluded that optimal designs are obtained by matching, as much as practically possible, the external and internal stiffness. This matching must be done from below, i.e., by keeping the apparent external stiffness of the application lower than, or equal to, the internal stiffness of the ISA actuator.

<table>
<thead>
<tr>
<th>Table 1</th>
<th>Typical Parameters for ISA Stacks (PI-247-70 Actuator(^2))</th>
</tr>
</thead>
<tbody>
<tr>
<td>Maximum displacement</td>
<td>0.120 mm</td>
</tr>
<tr>
<td>Length</td>
<td>144 mm</td>
</tr>
<tr>
<td>Internal stiffness</td>
<td>370 kN/mm</td>
</tr>
<tr>
<td>Volume</td>
<td>163 (10^{-6}) m(^3)</td>
</tr>
<tr>
<td>Weight</td>
<td>1 kg</td>
</tr>
<tr>
<td>Maximum free strain</td>
<td>0.083 %</td>
</tr>
<tr>
<td>Maximum deliverable energy per unit volume</td>
<td>4.078 kJ/m(^3)</td>
</tr>
<tr>
<td>Maximum deliverable energy per unit mass</td>
<td>0.666 J/kg</td>
</tr>
</tbody>
</table>

2 Review of Existing Proposals for Individual Blade Control

Several proposals have recently been developed for achieving individual blade control (IBC) through conventional and unconventional means. These proposals range from the direct distributed control of twist along the blade span, to the use of aerodynamic control surfaces discretely placed at the blade outer stations. Application of ISA technology in rotary wing IBC follows similar developments taking place in the ISA control of conventional wings, and a significant degree of technological cross-fertilization is taking place.

2.1 Induced Twist Concept

ISA control of bending and twisting seems obviously the most direct and potentially effective way of aeroelastic control. Theoretical studies on ISA adaptive aeroelastic control were first done on classical wings, and then extended to rotor blades.

![Figure 3](image)

Figure 3  Diagonal PZT twist actuation concept.\(^14\)
Theoretical studies of ISA twist control of rotor blades conducted at DLR in Germany\textsuperscript{12,13} showed the benefits of spanwise twist distribution over discrete control surface movement, especially when wide-band reduction of rotor systems vibration is sought. However, numerical simulations with existing PZT technology have shown that only the vibration modes with very low aerodynamic damping will benefit. Hence, it was concluded that the practical implementation of the method relies on “future development of a new generation of PZT materials having higher strain/volt capabilities”.\textsuperscript{13}

\begin{figure}[h]
\centering
\includegraphics[width=0.5\textwidth]{image.png}
\caption{Twist and bending slopes vs PZT angle.\textsuperscript{14}}
\end{figure}

A 1/8 Froude scale composite rotor was constructed at the University of Maryland.\textsuperscript{14} It used diagonally oriented PZT crystals embedded at 45° in the fiberglass skin (Fig. 3) to achieve induced twist when an electric field was applied. A value of 1° tip rotation was targeted in these trials. Extensive theoretical and experimental studies were conducted. Good prediction of tip twist and bending slopes was reported\textsuperscript{14} (Fig. 4). The largest recorded values, in agreement with predicted results, did not exceed 0.35°. Dynamic tests were also performed in non-rotating and rotating conditions. Significant twist response was measured when excitation was close to resonant bending or torsion frequencies (50 Hz and 95 Hz, respectively). Maximum tip twist values at these frequencies were 0.35° and 1.1°, respectively. Outside resonance, a very small response was observed. These experimental results seem to confirm the theoretical conclusion\textsuperscript{13} that the practical implementation of induced twist actuation through embedded PZT technology is difficult.

\subsection*{2.2 Servo-flap Concept}

The servo-flap concept has received considerably more attention due to the inherent advantages of extracting additional power and energy from the airstream through the servo-aerodynamic effect. This concept was first pioneered by Kaman Aerospace Corp.\textsuperscript{8}. The helicopters SH-2G Seaprite and K-Max are flying proof of its feasibility. However, these two helicopters still employ a conventional swashplate to achieve control of the servo-flap through levers and control rods running through the blade. The purpose of recent studies was to replace these mechanical controls with other means (electrical, electro-hydraulic, etc.).

\subsubsection*{2.2.1 Advanced Rotor Control System (ARCS) Studies}

Conventional electrical motors and electro-hydraulic actuators were considered in an extensive industry study of the advanced rotor control system (ARCS) to be implemented on existing helicopters such as the McDonnell Douglas Helicopter Co. (MDHC) AH-64A Apache\textsuperscript{15}, and the Helicopter Textron Inc. (BHTI) AH-1W Super Cobra\textsuperscript{16}. Extensive studies ranging from aerodynamic predictions to detailed engineering design were performed. The results showed consistently that if the servo-flap concept were to be used for achieving both basic flying controls (collective and cyclic pitch) as well as HHC-IBC, then extensive flap travel and hinge moment capabilities would be required. The MDHC study\textsuperscript{15} used previous experience\textsuperscript{3} and the CAMRAD-JA prediction code to evaluate a 42.5 % chord, 17.5 % span (0.65R - 0.825R) flap design extending 32.5 % beyond the trailing edge of the blade. The peak deflection and hinge moment values resulting from this study were placed around +/− 19.2°, and 2445 lb-in, with an associated electrical power of 15 Kw per blade. The BHTI study\textsuperscript{16} used the COPTER aerodynamic code to evaluate a servo-flap design of slightly different dimensions: 20 % chord, 20 % span centered about the 0.70 spanwise blade station. This study\textsuperscript{16} yielded peak values of ±18.9° and 1200 lb-in, with 1.95 Kw per blade. The nearly 8 times difference in power values between the two predictions points out the sensitivity of this kind of analysis on modeling methods, engineering solutions, and design selections.

\begin{figure}[h]
\centering
\includegraphics[width=0.5\textwidth]{image2.png}
\caption{Advanced rotor control system (ARCS) servo-flap actuation concepts: (a) electric motor (McDonnell Douglas Helicopter Co.\textsuperscript{15}); (b) hydraulic cylinder (Bell Helicopter Textron Inc.\textsuperscript{16}).}
\end{figure}

However, both studies pointed out that achieving both primary (collective and cyclic) and vibration control through the same actuation system does not lead to an efficient design. Hence, the alternative of separating primary control from vibration control was contemplated, and either collective, or both collective and cyclic controls were achieved through conventional root actuation. Thus, the requirements imposed on the servo-flap system could be brought within more feasible bounds. The HHC-IBC
values estimated by these studies as: ± 0.90°, with 512 lb-in at 24.1 Hz for MDHC (flap reduced to 35% chord, 15% span), requiring 512 W per blade; and ± 3.8°, with 320 lb-in at 20 Hz for BHTI (same flap as before), requiring 388 W per blade.

2.2.2. ISA Servo-flap Studies

2.2.2.1 Theoretical studies at UCLA

Extensive theoretical studies performed at UCLA\(^\text{17}\) showed the benefits of using the servo-flap concept for active control of helicopter rotor blades. Both spring-restrained rigid blade and fully elastic blade models were used. Geometrical non-linearities were included. Advanced unsteady aerodynamic 2-D models were employed, together with a flap efficiency coefficient of 60% to compensate for previously reported discrepancies between theory and experiments. A vibration reduction controller was connected with the aeroelastic model, and HHC input to the flap was assumed. Substantial vibration reduction was demonstrated at various helicopter airspeeds corresponding to advance ratios \(\mu = 0.0 - 0.4\). The study provides estimates of the required flap travel and hinge moment. The average power consumption was also calculated using the formula\(^\text{17}\):

\[
P_{\alpha} = \sum_{k=1}^{n_k} \frac{1}{2\pi} \int_{0}^{2\pi} M_{\alpha}(\psi_k) \delta(\psi_k) \, d\psi_k,
\]

where \(P_{\alpha}\) signifies control surface power, \(n_{k}\) is the number of blades, \(M_{\alpha}(\psi_k)\) is the control surface hinge moment, \(\delta(\psi)\) is the time derivative of control surface deflection, and \(\psi_k\) is the azimuthal position of the \(k\)-th blade. Similar calculations were also obtained from conventional IBC blade root actuation, and comparisons of the servo-flap and conventional HHC-IBC were performed. Though the values varied with the blade modeling complexity, the conclusion could be drawn that substantially less power is required to implement HHC-IBC through the servo-flap concept than through the conventional root actuation. The numerical values required to achieve satisfactory HHC-IBC were found to vary with the rotational and torsional stiffness of the blade. The softer (lower frequency) blades were easier to control through the servo-flap system. In this case (\(\omega_r = 3/\text{rev}\)), it was found\(^\text{17}\) that the peak deflections of the flap should be \(+3^\circ/-4^\circ\). No values were given for the corresponding hinge moment. The corresponding power requirement was found to be around 0.25% of the rotor power.\(^\text{17}\) The flap deflection and power requirements for the torsionally stiffer blade (\(\omega_r = 5/\text{rev}\)) were considerably higher.

2.2.2.2 Theoretical and experimental studies at MIT

Extensive theoretical and experimental studies of ISA controlled flaps were performed at MIT. In one investigation\(^\text{18}\), bimorph arrangements of PZT material were used to produce a lateral displacement that was transformed into flap rotation through a hinge-and-lever mechanism (Fig. 6). The studies were targeted at the Boeing CH-47D tandem helicopter having a large (\(R = 30\) ft) 3-blade rotor rotating at 225 rpm (3.75 Hz). HHC input at 3/rev (11.25 Hz) were considered in the theoretical study, and extensive scaling and modeling work was performed in order to obtain experimental data. A 1/5 scale wind tunnel test was set up using a stationary blade section equipped with a 10% chord flap. A 1/10 velocity scale and 1/2 frequency scale was employed in the model design. Experiments were successfully conducted at various airspeeds between zero and 78 ft/sec, and at frequencies up to 100 Hz. The flap deflection capability, as well as the resulting lift and pitch moment coefficients created by this deflection, were observed and discussed.

![Bimorph PZT flap actuator](image)

Figure 6 Bimorph PZT flap actuator.\(^\text{18}\)

As a general trend, values significantly below the theoretical predictions were reported. This discrepancy was attributed to certain inconsistencies in Reynolds’ number (and hence boundary layer thickness), as well as to high mechanical losses due to friction in the hinges, the low stiffness of the trailing edge design, and the spanwise bending response of the model.

Another study was independently conducted by SatCon Technology Co. in cooperation with MIT\(^\text{8}\) to design an ISA rotor blade flap actuator using the magnetostrictive TERFENOL-D material for the Sikorsky UH-60A Black Hawk helicopter. Analytical predictions of flap angle, hinge moments and control power requirements, as well as ISA actuator sizing based on the peak energy needs were reported. The requirement of \(\pm 2^\circ\) for conventional (root actuation) HHC-IBC was taken as basis, and then translated into equivalent flap deflection based on flap chord ratio and spanwise dimension and location. It was found that, for a full span flap, the required deflection varies from \(\pm 5^\circ\) with a 10% chord, to \(\pm 3^\circ\) with 40% chord. Adjustment for the partial span flap was done using the percentage positions of the inner and outer ends of the flap (\(\epsilon_{in}\) and \(\epsilon_{out}\), respectively) in the formula:

\[
\delta_\alpha = \frac{1}{\epsilon_{out} - \epsilon_{in}} \delta_f,
\]

where \(ps\) and \(f\) signify "partial span" and "full span", respectively. Eventually, a 17.5% chord, 46% span (0.52R - 0.98R) flap was selected, requiring a travel range of \(\pm 5.7^\circ\). The corresponding aerodynamic hinge moment was estimated at \(\pm 30\) ft-lb, with an additional constant contribution of 11 ft-lb being added to the positive side to account for steady state maneuver loads. Next, the peak
energy required for achieving these deflection-moment values was calculated, and it was found to be 3.5 J for the positive side, and 2.0 J for the negative side of the cycle. With a 50% margin of error assumption, a final design based on 6 TERFENOL-D actuators per blade was achieved. The estimated electrical power consumption was 7.2 Kw (i.e., 1.8 Kw per blade). The combined weight of all the actuators was 43 kg (i.e., 10.75 kg per blade). These values were shown to be well within the accepted power and weight budgets of the complete actuation system taken as 1% of helicopter power and weight (11.2 W and 81 kg, respectively).

2.2.2.3 Experimental and theoretical studies at University of Maryland
Experimental and theoretical studies with the bimorph ISA principle for rotor blade flap actuation (Fig. 7) were conducted at the Center for Rotorcraft Education and Research at the University of Maryland19.

![Image](image_url)

**Figure 7** Parameter study of bimorph effectiveness: (a) device schematic; (b) linkage arm and actuator capability plots.19

Trailing edge flaps of 20% chord, 12% span (0.85R - 0.97R) were built into a 36-in radius, 3-in chord composite blade model. Initial tests were performed with the stationary blade placed in a conventional wind tunnel operated at speeds up to 111 ft/sec, and at two airfoil set angles (4° and 8°). Excitation at frequencies up to 15 Hz showed good frequency response, but a very significant decrease in amplitude with increasing airspeed. Further experiments were conducted with the blade installed in a rotating rig operated at up to 900 rpm (15 Hz). Excitation frequencies of 1, 2, 3, and 4/rev were investigated. The trailing edge flap response was to be rather constant with frequency, but strongly decreasing with rpm. The blade flapping response varied with excitation frequency and rotor speed due to interdependence between flapping resonant frequency and rotor rpm. To counteract the decrease in trailing edge flap response with airspeed, further investigation was conducted with 2-layer and 4-layer bimorph PZT designs19. The use of the 2-layer actuator generated an increase of about 21% in the flap angle at the higher speed value. The 4-layer actuator showed much improved force capability, but lower displacement"19. The goal of these investigations was initially set at achieving 2° of flap deflection at 258 ft/sec airspeed. Subsequently, the goal was redefined as "5% flap authority (additional steady lift due to a flap deflection divided by total steady lift with zero flap deflection) at 8° collective blade pitch"19. A theoretical parameter study was performed19 to determine the required linkage arm length that will meet this specification (Fig. 7). At present, a second experimental rotor with a larger span and multi-layered actuators is being built and will be tested on the hover stand for a range of rotor speeds and collective settings.

3 Analysis of Rotor Blade Servo-flap Vibration Control with ISA Devices

Various attempts reported in the literature regarding the study and experimentation of Individual Blade Control (IBC) through conventional (electro-mechanical, hydraulic) and solid state (electro-mechanical ISA) means were reviewed in the previous section. Two major directions are apparent: the direct twist of the rotor blade through tension-torsion-bending coupling, and indirect modification of the aerodynamic lift and pitch moment of the blade using the servo-flap and servo-tab principles. The former concept is more direct and allows for properly addressing modal control, since continuous variation of induced strain effects along the blade length can be achieved. The latter concept has the shortcoming of an additional complication in the rotor construction, and uses a rather rigid device (flap or tab) with only limited span-wise extent; however, it is much more efficient in achieving a palpable result due to the amplification properties of servo-aerodynamics. For this latter reason, and considering the present day state-of-the-art in ISA technology, most engineering efforts reported in the literature have been generally directed towards the servo-flap concept, and hence, we shall restrict our investigation in subsequent chapters towards this option. However, one should recognize that as yet there is *nolo contendere* in passing judgement on the feasibility of the induced twist concept.

3.1 Definition of an ISA Servo-flap for Rotor Blade Vibration Control

Within the servo-flap concept, one observes that the requirements for rotor blade primary steady state and dynamic control (collective and cyclic pitch) cannot be simultaneously met by the same ISA device. Both collective and cyclic pitch controls require large servo-flap deflection authority (≈ ±20°) and are too demanding for existing ISA technology. Hence, our attention regarding the use of dynamic ISA devices for helicopter IBC will be restricted to vibration control, i.e. HHC applications.
Table 2 Proposed bench-mark design parameters for rotor blade vibration control ISA servo-flap system

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value and units</th>
<th>Notes</th>
</tr>
</thead>
<tbody>
<tr>
<td>Flap deflection</td>
<td>±1/30 rad (≈ 2°)</td>
<td>simultaneous with flap deflection</td>
</tr>
<tr>
<td>Hinge moment</td>
<td>±75 Nm</td>
<td></td>
</tr>
<tr>
<td>Frequency</td>
<td>25 - 30 Hz</td>
<td></td>
</tr>
<tr>
<td>Maximum instantaneous energy transmitted to the airstream</td>
<td>1.25 J / blade</td>
<td>(zero steady force is assumed)</td>
</tr>
<tr>
<td>Maximum instantaneous power transmitted to the airstream</td>
<td>0.5 kW</td>
<td></td>
</tr>
<tr>
<td>ISA actuator weight budget</td>
<td>10 kg / blade</td>
<td>≈ 10% of typical blade weight</td>
</tr>
<tr>
<td>ISA system weight budget (including power supply, lead wires, controls, etc.)</td>
<td>80 kg / 4-blade helicopter</td>
<td>≈ 1% of typical helicopter weight</td>
</tr>
<tr>
<td>Overall power consumption budget</td>
<td>10-12 kW / 4-blade helicopter</td>
<td>≈ 1% of typical rotor power</td>
</tr>
<tr>
<td>Specific transmitted energy</td>
<td>0.125 J/kg</td>
<td></td>
</tr>
<tr>
<td>Specific transmitted power</td>
<td>25 W/kg</td>
<td></td>
</tr>
<tr>
<td>Specific power consumption</td>
<td>≤140 W/kg</td>
<td>including all losses</td>
</tr>
</tbody>
</table>

In a previous study, we used the data reviewed in section 2 to identify baseline values for an ISA servo-flap system to be used for rotor blade vibration control. Though large variations were noticed, a benchmark set of values for flap deflection, hinge moment, frequency, mass, energy, and power, as well as energy and power densities could be estimated (Table 2). The ISA devices reviewed in Table 1, section 1.2, can deliver 0.666 J/kg and hence the 0.125 J/kg specific energy requirement of Table 2 can be met. (The weight of the power supply, lead wires, control electronics, etc. was not included in Table 1, but even with their contribution the overall trend will be the same.)

![ISA actuator diagram](image)

Figure 8 ISA servo-flap for rotor blade vibration control

Commercially available ISA stacks could be used to meet the specifications laid down in Table 2. The output energy requirement can be met by a double pair of ISA stacks, each pair 300 mm long, 40 mm diameter, and 1 kg mass, with free travel of ≈ 0.125 mm. Each pair is L = 300 mm long, and has a total free travel $x_A \approx 0.250$ mm. Hence 4 kg of ISA mass is required for each blade. Adequate weight allowance for system implementation must be made.

![Gain G, k_e, k_i, k_d, x_A, X_D, X_i, X_{O}, Control Surface with δ, ISA input, Internal Stiffness, External Stiffness, X_e, X_{Output}](image)

Figure 9 Schematic drawing of displacement amplification principles.

We propose to allow 10% of the blade weight (i.e., 10 kg / blade) for the ISA actuator, servo-flap linkages, and local reinforcement of the blade section.
Figure 10 Proposed electro-aero-mechanical model for ISA servo-flap vibration control

For the complete system, including power supply, control electronics, etc., a larger weight allowance can be made, since part of it will be accommodated in the fuselage. Hence we propose that the complete system does not exceed 1% of the helicopter weight (i.e., 80 kg / 4-blade helicopter).

Since the ISA travel is too small for practical applications, a displacement amplifier is affixed to the end of the ISA stack. (The ISA stack and the displacement amplifier make up the actuator.) A practical actuator output of 1 mm is desired since this value leads to a hinge arm r = 30 mm to produce the required flap deflection δ = 1/30 rad. Suitable values for the amplification gain G can be selected to achieve the η = 4 amplification required to increase the 0.250 mm ISA displacement to the required 1 mm actuator output. A formula can be derived for finding the optimal gain values using feedback principles:22

\[ G(\eta) = \frac{1}{2\eta} \left( \frac{k_i}{k_e} - \frac{k_i}{k_e} - 4\eta^2 \right) \quad (3.1) \]

This formula ensures that optimum use is made of the ISA capabilities for a given pair of internal and external stiffness values, \( k_i \) and \( k_e \). In this particular application, the ± 1 mm output target means a 4:1 displacement ratio and yields the value \( G = 5.83 \) for the optimal gain.

3.2 Aero-electro-mechanical Modeling of the ISA Servo-flap System

Figure 10 presents a conceptual electro-aero-mechanical model that can be used to study the actuation scheme shown in Figure 8. It contains several distinct parts: the electrical impedance associated with the ISA device; the mechanical impedance of the support system; the mechanical impedance of the control system and control surface; and the equivalent mechanical impedance simulating the reaction of the airstream. A feedback sensor is positioned conveniently to pick up the servo-flap response. Its signal is fed into the control loop of basic gain G and feedback gain H. The predominant electric parameter of the ISA stack is its capacitance C. The resistance R models the electrical losses inside the ISA material, while the inductance L is designed to compensate the capacitive reactance and hence reduce the reactive power requirements. For a system operating at around a given frequency \( \omega \), the inductance values can be estimated such as to minimize the reactance

\[ X(\omega) = \omega L - \frac{1}{\omega_0 C} \quad (3.2) \]

Application of this concept (also known as power factor correction) to piezoelectric actuators was discussed.24 The counter-electro-motive force \( e \) represents the piezoelectric effect which appears as an electric reaction when the actuator is operating under mechanical load. (A similar effect takes place in conventional electric motors.)

The actuator mechanical impedance can be calculated using its dynamic stiffness, \( k_{\text{act}} = (1 + i \eta_{\text{act}}) k_{\text{act}} \). For modeling purposes, it is sometimes convenient to replace the internal loss coefficient \( \eta_{\text{act}} \) with an equivalent viscous coefficient \( c_{\text{act}} \). The actuator impedance depicted in this model also includes the effects due to the displacement amplification device incorporated into the actuator. The control circuit and control surface impedance incorporates the stiffness, damping and mass effects of both the servo-
flap and its hinge arm and supports.

The aerodynamic impedance incorporates equivalent aerodynamic mass, damping and stiffness associated with the oscillatory movement of the servo-flap. Their estimation is not straightforward, and includes non-steady aerodynamics, transonic effects on the advancing blade, and stall and reverse flow on the retreating blade. For first analysis, Theordersen's linear theory may be used.\textsuperscript{17,18}

Hence, the servo-flap hinge moment takes the form:

$$M_s(\omega) = [-\omega^2 m_{\text{aero}}(k) + i\omega c_{\text{aero}}(k) + k_{\text{aero}}(k)] \delta,$$  \hfill (3.3)

where \( k = \omega b/U \) is the reduced frequency. The aerodynamic stiffness, mass and damping depend on the reduced frequency \( k \), and hence their values vary depending on the flap geometry, local airspeed, and frequency.

\section{4 Discussion}

The work presented herein is far from being complete. Though a benchmark set of values and an electro-aero-mechanical model have been proposed, several avenues are still open for further research. First, a more precise definition the base-line requirements for achieving HHC-IBC vibration control of helicopter rotors should be investigated. This is a formidable analytical effort, since load prediction codes for rotor blade unsteady aerodynamics still show large variation in results. However, a concerted effort of industry and academia could yield better estimates for the HHC-IBC actuator than the "rough-and-ready" approximations proposed herein.

Second, the present analysis should be extended to non-ideal displacement amplifier incorporating transmission losses due to their own compliance and damping. Third, the more comprehensive subject of identifying and numerically defining the power dissipation mechanisms involved in the three stages of an ISA system (ISA material, power supply, and mechanical and electrical transmissions) should be addressed in more detail. Fourth, no discussion was made here of the effects of D.C. bias field, and of the associated steady load, usually present in all ISA devices (This offset is necessary for at least two reasons: (a) to avoid putting the ISA material into tension; (b) to compensate for the asymmetric tension-compression behavior of the ISA material). Finally, a major concern with any rotor blade device operating in the outer blade span is the effect of very high \( g \) loading (a point at 5 m radius rotating at 30 rad/sec experiences \( R\Omega^2 = 5 \times 30^2 = 4500 \text{ m/s}^2 = 450 \text{ g} \)). This was a major concern with the ARCS studies\textsuperscript{15,16} considering conventional electro-mechanical and hydraulic devices. Regarding the effect of \( g \) loading on ISA devices, very little theoretical and experimental research has been reported so far. Though the solid-state nature of an ISA device justifies optimism in this respect, properly conducted studies are still required.

\section{5 Conclusions}

A review of the existing literature produced baseline requirements for implementing active HHC-IBC vibration alleviation using ISA devices. Typical values of \( \pm 2^\circ \) servo-flap displacement, and \( \pm 75 \text{ Nm} \) of hinge moment at 25-30 Hz were identified and proposed as benchmark values for future investigations. Maximum instantaneous energy of 1.25 J and maximum instantaneous power output of 0.5 kW, per blade, were also identified. Weight budget allowance of 10 kg/blade for the actuators and linkages, and 80 kg / 4-blade helicopter for the complete ISA system are proposed, along with corresponding power budget values. The resulting energy density requirement of 0.125 J/kg was found within the capabilities of commercially available ISA stacks. However, displacement amplification devices are necessary at the ISA stack level. Using an optimal design for the displacement amplifier, realistic values for actuator output (1 mm) and servo-flap hinge arm (30 mm) could be considered.

An electro-aero-mechanical model is proposed for the analysis of an ISA servo-flap for rotor blade vibration control. Opportunities for further research are identified. The experimentation of an actual ISA device coupled with an optimally designed displacement amplifier would substantiate the main conclusions of this study and identify practical issues associated with its real-time implementation. The baseline specification developed in this and previous studies\textsuperscript{22} is also proposed as a benchmark for testing other possible ISA solutions.

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\section{7 References}

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