TITLE: Active/Adaptive Rotor Blade Control for Disturbance Rejecton and Performance Enhancement

Active/Adaptive Rotor Blade Control for Disturbance Rejection and Performance Enhancement

R. Kube
Deutsches Zentrum für Luft- und Raumfahrt
Lillenthalplatz 7
D-38108 Braunschweig, Germany

D. Schimke
Eurocopter Deutschland
81663 München

P. Jänker
DaimlerChrysler
81663 München, Germany

Abstract

As part of a program for development of active/adaptive rotor systems, flight tests were performed with a helicopter featuring a powerful blade root actuation system and a comprehensive sensor instrumentation. The test results gave a deep insight in the mechanisms involved in the reduction of BVI noise emissions and fuselage vibrations through 2/rev blade root control inputs and allowed the derivation of an algorithm for online estimation of the BVI noise emissions experienced on ground. In combination with a closed loop control law it yields a system which allows an automatic reduction of the rotor disturbances on the basis of local blade surface pressure measurements. The closed loop system can also be applied to a trailing edge flap which has been realized and investigated in the Transonic Wind Tunnel Göttingen (TWG) in the second part of the active/adaptive rotor development program. The test results demonstrated the ability of a servo flap to twist the blade to a degree which is required for rejection of the rotor disturbances and enhancement of the rotor performance.

1. Introduction

Due to the azimuthal and radial variation of the flow velocity, the blades of a helicopter rotor experience within one revolution, the fuselage vibrations are comparatively high throughout the complete flight envelope. In descent flight the vibrations are accompanied by strong noise emissions originating from interactions of the rotor blades with the trailed vortices of the preceding blades. While the vibrations represent a considerable stress for the material and, in addition, lead to a reduced flight comfort for passengers and crew on board the aircraft, the **B**lade **V**ortex **I**nteraction (BVI) noise is very annoying for the population on ground.

Both disturbances can be reduced by means of active and adaptive rotor control respectively, techniques which are actually subjected to intensive investigations not only in the U.S. but also in Japan, for example. Although having no long tradition in helicopter development, the Japanese heavy industries started to realize trailing edge flap systems immediately after having initiated their rotor design activities [1]. In the U.S. a lot of experimental and numerical experience on the field of active and adaptive rotor control was gathered in the past 10 years [2, 3] which now forms the basis for the development of a piezo-electric driven trailing edge system [4] and its application to a full-scale helicopter rotor in free flight [5].

A comparable program consisting of wind tunnel, whirl tower and flight tests is jointly conducted by Eurocopter Deutschland (ECD), DaimlerChrysler (DC) and Deutsches Zentrum für Luft- und Raumfahrt (DLR) in Germany. Although its main objective is the realization of a trailing edge flap system, too, the program was accompanied in its first phase by flight tests with **A**ctive **R**otor **C**ontrol (ARC) in a form also known as **I**ndividual **B**lade **C**ontrol (IBC). The IBC flight tests aimed on the generation of a data base which allows an identification of the mechanisms involved in active rejection of the rotor disturbances and the derivation of suited closed loop control laws.

2. Flight Tests with Active Blade Root Control

2.1 Open Loop Noise and Vibration Reduction

2.1.1 Experimental System

The IBC flight tests were conducted under participation of Zahnradfabrik Luftfahrttechnik (ZFL), the manufacturer of hydraulic actuators suited for substitution of the conventional pitch links in the rotating frame. As test bed served a BO105 helicopter (fig. 1) which featured a comprehensive sensor system and blade instrumentation. The latter one comprised pressure transducers, accelerometers as well as strain gauges in order to allow an investigation of the blade surface pressures, the tip displacement as well as the blade bending in flap and lead-lag direction.

The sensor signals were A/D converted in the rotating frame with a resolution of 512/rev before being transmitted via transputer link to the on board data acquisition system IBIS. IBIS merged the data stream of the rotating system with the signals originating from the basic instrumentation and stored them on an 80 MB silicon disc.

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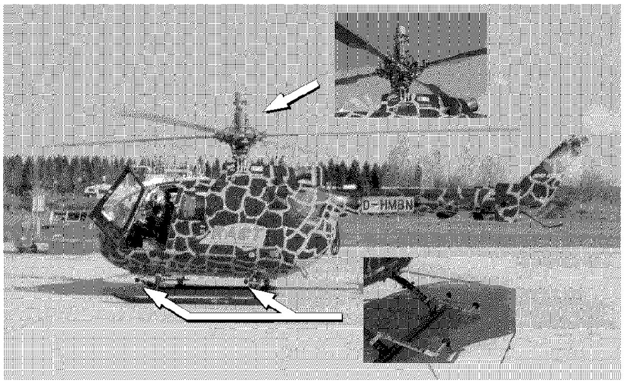

Fig. 1 BO105-S1 Test Helicopter

Simultaneously the noise experienced on ground was measured by means of a microphone array set up perpendicular to the flight path. The array consisted of 11 microphones and covered a range of ±300m. 15m apart from the center microphone a photo camera was erected in order to determine the overflight height and time. The flight trajectory was measured by means of a GPS receiver installed on board the aircraft [6].

The test matrix covered a wide range of BVI flight conditions with a systematic variation of IBC settings for numerous advance ratios and descent angles. The nominal test point was a 6°-descent flight with 65 knots. At this condition 2-, 3-, 4- and 5/rev blade pitch angles were superimposed individually and in a combined way to the conventional control inputs of the pilot. While the 3/rev amplitude was limited to 0.5°, the 2/rev blade pitch angle as the most interesting one was generated with an amplitude between 0.4° and 1°.

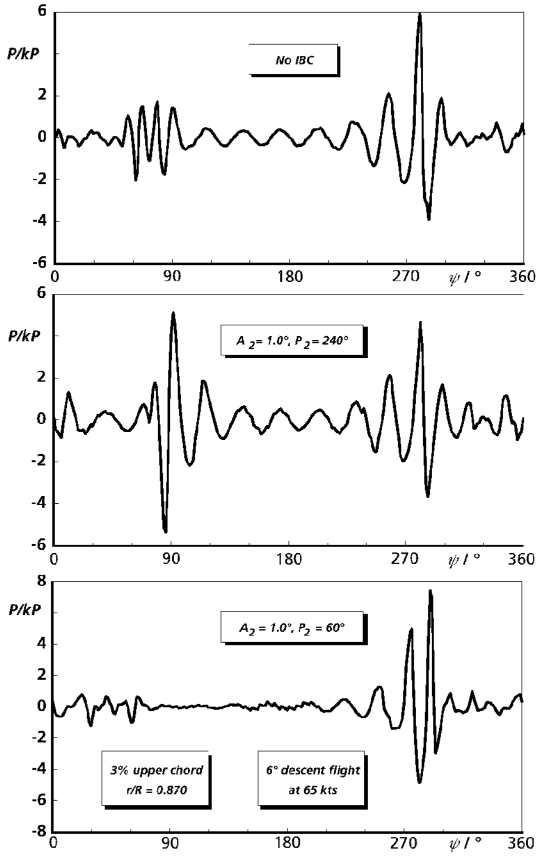

Fig. 2 Time History of High Pass Filtered Leading Edge Pressure

2.1.2 BVI Noise Reduction

The IBC flight test results demonstrated the strong IBC potential for avoiding blade vortex interactions. They obviously take place between 60° and 80° rotor azimuth and are reduced in number when a 2/rev blade pitch angle of 1.0° amplitude and 240° phase shift is applied (fig. 2). The few remaining blade vortex interaction occurring at approximately 90° rotor azimuth, however, yield pressure fluctuations of high intensity indicating that the rotor blade encounters with vortices of high strength at this position.

However, compared to baseline case, where the rotor blades perform a parallel interaction with the vortex filaments in the first quadrant of the rotor disc, non-parallel blade-vortex interactions take place for a 2/rev blade pitch angle of 1° amplitude and 240° phase shift (fig. 3). Since this modification of the interaction geometry yields a minimization of the interaction length, the rotor noise emissions are considerably reduced despite of a high vortex strength.

The mechanism leading to this modification of the interaction geometry can be derived from fig. 4 which shows the low pass filtered leading edge pressure distribution assumed to be proportional to the blade lift in a first step. From fig. 4 it can be seen that, compared to the baseline case, lift is obviously increased in the second quadrant of the rotor disc when a 2/rev blade pitch angle is adjusted with a phase shift of 240°. Although this increased blade lift is probably associated with a high vortex strength, it also yields a strong downwash which moves the BVI noise relevant vortices more downwards thus increasing the blade-vortex miss distance in the first quadrant of the rotor disc.

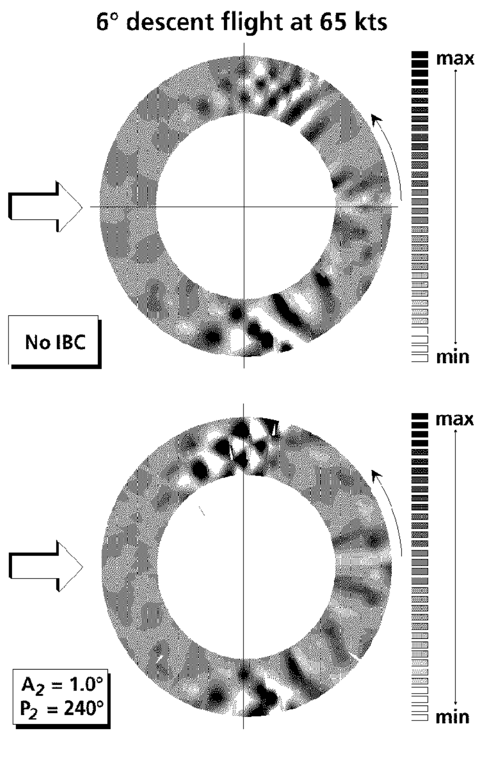

Fig. 3 Interaction Geometry Derived from High Pass Filtered Leading Edge Pressure

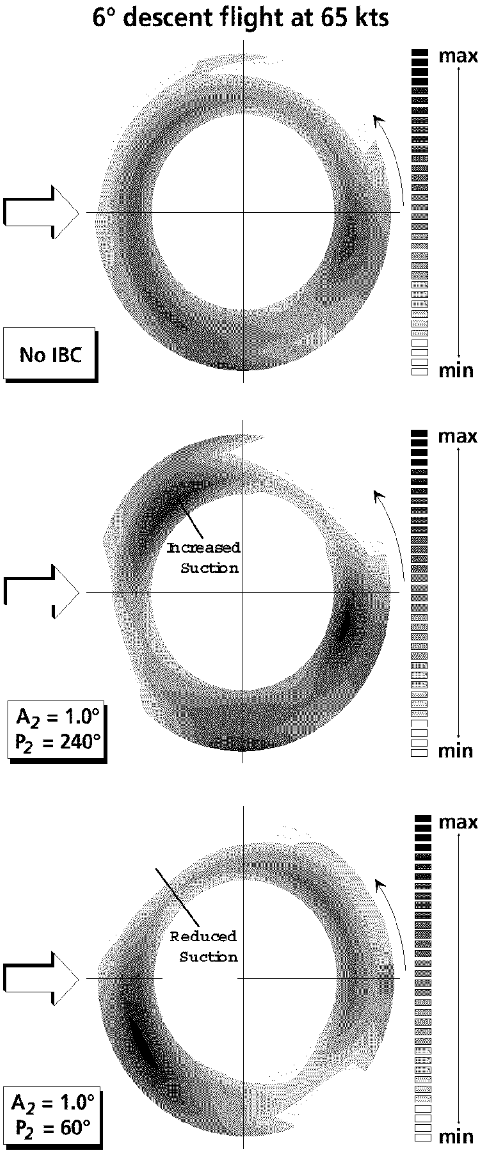

Fig. 4 Low Pass Filtered Leading Edge Pressure

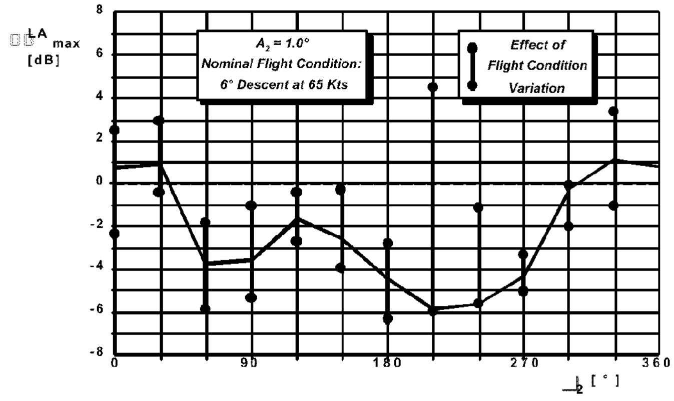

Fig. 5 BVI Noise Reduction due to 2/rev Blade Pitch

The opposite trend can be observed when the 2/rev phase shift is adjusted to 60°. In this case lift is obviously reduced in the second quadrant (fig. 4) thus yielding vortices of reduced strength. Since the lift becomes partially even negative, an induced upwash occurs within this area which can be assumed to increase the blade-vortex miss distance by moving the vortices above the rotor disc. As a result, blade vortex interactions are avoided and the pressure fluctuations between 60° and 80° rotor azimuth occurring in baseline case are dramatically reduced (fig. 2).

Due to the low vortex strength, the achieved BVI noise reduction is fairly insensitive to changes of the flight condition in this case. Therefore, the BVI noise level remains by at least 2dB below the baseline case for all flight conditions close to the nominal 6° landing approach even though amplitude and phase shift of the 2/rev blade pitch angel were fixed to 1° and 60° respectively (fig. 3).

In opposition to these findings, the noise reduction achieved with a 2/rev phase shift of 240° turned out to be very sensitive with respect to flight condition. Due to the high vortex strength occurring in this case, a reduction of the blade-vortex miss distance via a modification of the helicopter pitch or roll angle, for example, has a strong effect on the rotor noise emissions (fig. 5). Therefore a 2/rev phase shift of 240° may increase the BVI noise level by 5 dB while simultaneously having the potential for a reduction by up to 6 dB.

2.2 Vibration Reduction

According to [7], for example, a reduction of the vibrations generated by a 4-bladed rotor requires the superposition of a 3-, 4- and 5/rev blade pitch angle to the conventional one. While its 4/rev part theoretically affects the vertical fuselage vibrations only, the 3- and 5/rev part acts in lateral and longitudinal direction. However, this assumption is based on an infinite blade stiffness and therefore does not take into account the rotor dynamics.

They have turned out to play a major role in case of vibration reduction through active blade root control. For the hingeless BO105 rotor, for example, a strong 3/rev blade excitation was noticed and identified to correlate clearly with the 4/rev fuselage vibrations. The suppression of this 3/rev blade excitation by means of a 3/rev blade pitch angle, lead to a vibration reduction in all degrees of freedom [7].

The same could be achieved during the IBC flight tests which showed that the axial, lateral and vertical gearbox accelerations are affected simultaneously by a 3/rev blade pitch angle (fig. 6). Suprisingly, a 2/rev blade pitch angle turned out to have this potential, too. Although being not able to affect the 4/rev fuselage vibrations directly [8], the axial, lateral and vertical gearbox accelerations were diminished simultaneously within the S1 experiments (fig. 7). Since this vibration reduction in all degrees of freedom coincides, in addition, at least with a local BVI noise minimum (fig. 5), a 2/rev blade pitch angle is very attractive for applications aiming on a simultaneous BVI noise and vibration rejection with single-frequency control inputs.

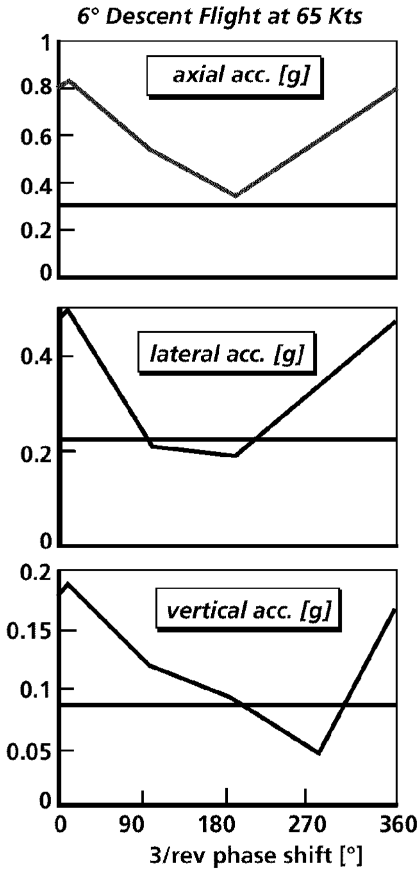

Fig. 6 Effect of 3/rev Blade Pitch Variation on Fuselage Vibrations

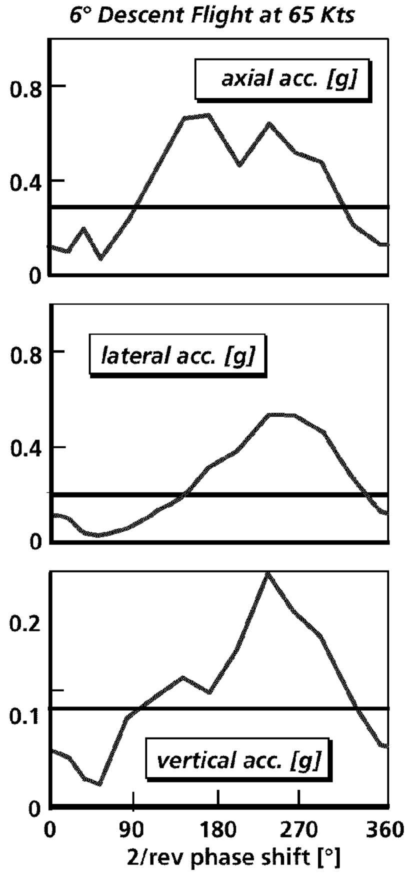

Fig. 7 Effect of 2/ref Blade Pitch Variation on Fuselage Vibrations

Multi-frequency inputs are obviously required for a reduced vibration level being associated with the absolute BVI noise minimum. Although this combination represents the optimum from both, the vibration and BVI noise point of view, it requires a closed loop control algorithm which is fast enough to keep the BVI noise emissions minimal even in case of their sensitivity to changes in flight condition.

3. High Order Control Law for Closed Loop BVI Noise and Vibration Reduction

3.1 Disturbance and Plant Characteristics

Considering a steady-state flight condition in a first step, the vibrations and noise emissions representing the disturbances to be suppressed are of periodic nature and mainly consist of so called rotor harmonics (fig. 8, 9). While the blade vortex interaction noise has a frequency content of 24/rev to 160/rev, the vibrations are of the 1[st]. 2[nd], 4[th] and 8[th] rotor harmonic.

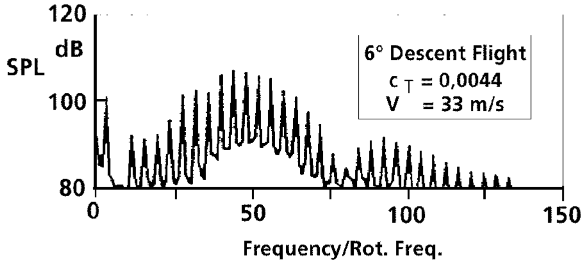

Fig. 8 Frequency Content of Rotor Noise

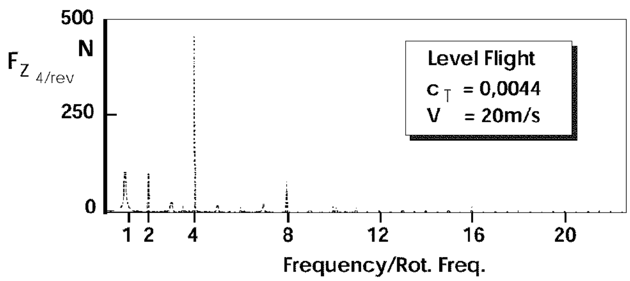

Fig. 9 Frequency Content of Vibrations

Due to their periodic nature the described disturbances can be represented by means of Fourier series which are characterized by their Fourier coefficients. The vibrations are dominated by integral multiples of the blade passage frequency for what reason the corresponding Fourier series becomes of the form

$$F_{FL} = \sum_{n=1}^{\infty} a_{nb} \sin(nb\psi) + b_{nb} \cos(nb\psi)$$

with

F_{FL}	vibratory force acting on fuselage,
ψ	rotor azimuth,
b	number of rotor blades,
a_{nb}, b_{nb}	Fourier coefficients

and

n	integral number.

For simplicity, the vibratory forces and moments acting on the rotor hub are usually combined to the vibration intrusion index

$$J_{Vib} = \sqrt{\frac{F_{x4}^2}{N} + \frac{F_{y4}^2}{N} + \frac{F_{z4}^2}{N} + \frac{M_{x4}^2}{Nm} + \frac{M_{y4}^2}{Nm}}$$

with

F_{x4}, F_{y4}	4/rev inplane rotor forces,
F_{z4}	4/rev out of plane rotor forces

and

M_{x4}, M_{y4}	4/rev pitching and rolling moment

As is shown exemplarily in fig. 10 this intrusion index keeps fairly constant from one rotor revolution to another and only varies slowly with flight condition.

For the noise emissions the Fourier series becomes of the form

$$J_{BVI} = \sum_{n=n_{min}}^{n_{max}} a_n \sin(n\psi) + b_n \cos(n\psi)$$

with

J_{BVI}	BVI noise intrusion index

and

n_{min}, n_{max} integral numbers.

Although the noise intrusion index varies with flight condition, too, it also fluctuates strongly from one rotor revolution to another (fig. 11). These fluctuations can be assumed to be due to small changes of the local profile aerodynamics and the downwash geometry respectively which have a strong effect on the noise emissions when occurring at noise relevant rotor azimuth positions [9].

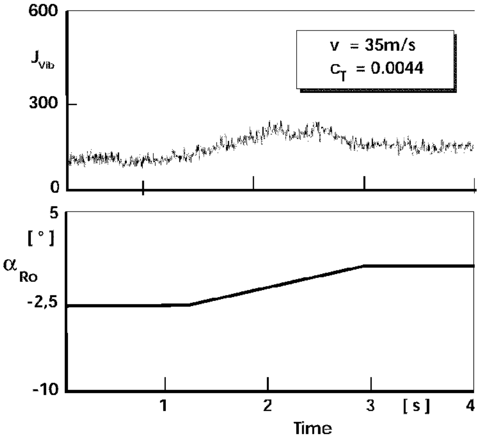

Fig. 10 Variation of Vibration Intrusion Index

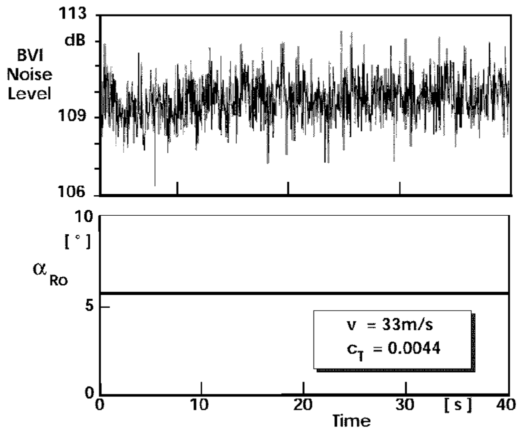

Fig. 11 Variation of BVI Noise Level

A closed loop controller for reduction of the rotor disturbances therefore needs to work with a high gain on the one hand in order to be fast enough for compensation of the strong BVI noise level changes occurring even in case of small flight condition variations. On the other hand low gain is required in order to avoid strong controller reactions to the feedback signal's fluctuations and possible amplification. In principle, this can best be achieved by means of robust control in the time domain where the disturbances are directly fed back to the closed lop controller. The feedback gains are of constant type and originate from an offline design procedure aiming on the realization of a minimum step response time, for example. Although this type of controller has the strongest potential to succeed in a reduction of the rotor disturbances despite of the strong fluctuating feedback signals, it runs the risk to become instable in case of rotor transfer function changes due to variations of the flight conditions. This disadvantage can be eliminated by using an adaptive controller algorithm for which a very satisfying controller performance throughout the complete flight envelope can be expected. Disadvantageous, however, is the high amount of mathematical operations to be performed online in addition to the control process itself. Since these operations have to be performed within a time interval which is inverse proportional to the feedback signal dynamics, an adaptive control algorithm working in time domain can hardly be realized for disturbances with high frequency content like rotor noise and vibrations, for example.

Realized, however, can be an adaptive closed loop controller for noise and vibration reduction which works in the frequency domain. It needs to take into account only a few of the feedback signal harmonics, for what reason the computational effort for the transformation from time to frequency domain by means of a recursive harmonic analysis and from frequency to time domain by means of a harmonic synthesis can be kept small. The resulting Fourier coefficients of the feedback signal harmonics vary comparatively slowly with the ones of the vibrations being mainly affected by changes of the flight condition.

The Fourier coefficients of the rotor noise harmonics, however, are in addition very sensitive to atmospheric disturbances, for what reason they fluctuate strongly from one rotor revolution to another. Since the dynamics of these fluctuations are much lower than the ones of the noise emissions themselves, a frequency domain controller represents a very promising solution for an active reduction of these disturbances. It can be operated at a low rate without running the risk to decrease the controller performance at least in steady state. In order to achieve, in addition, a satisfying transient behavior of the closed loop system, the control law has to be of high order with the feedback gains not being adjusted during the control process according to an online identification of the rotor transfer function. Since the result of this process only represents an estimate of the real value, it is affected at least by small errors which may mislead or even destabilize the controller temporarily. Therefore a minimum step response time can only be achieved by a frequency domain controller which is of high order and, in addition, works with constant gain settings.

4. Control Law Design Procedure

4.1 Classical Approach

Due to the periodic characteristic of the rotor noise emissions and vibrations and the quasi-steady behavior of their harmonics, the design of a frequency domain controller can in principle be based on the so called T-matrix model [10, 11]. It establishes a linear relationship between the vector of active rotor control inputs and the disturbance vector and can be formulated either in global form

$$\underline{y}(k) = \underline{y}_0(k) + \underline{T}(k) \cdot \underline{\theta}_{ARC}(k)$$

with

$\underline{y}_0(k)$ disturbance vector in baseline case

and

$\underline{y}(k)$ disturbance vector in ARC case

or in local form

$$\Delta\underline{y}(k) = \underline{T}(k) \cdot \Delta\underline{\theta}_{ARC}(k)$$

with

$\Delta\underline{y}(k)$ vector of disturbance change

and

$\Delta\underline{\theta}_{ARC}(k)$ vector of ARC input change.

While the global model assumes linearity within the complete range of active rotor control inputs, the local model represents a linearization around the actual point of controller operation (fig. 12) and therefore also allows an approximation of nonlinear effects. The transients of the noise and vibrations due to a change of active rotor control inputs, however, are not taken into account because both models describe the rotor transfer function in a quasi-steady way via the T-matrix. Therefore a closed loop controller which is based on the T-matrix approach can not operate with a high frequency but needs to let the disturbance transients decay after a change of the active rotor control inputs before the next control input is determined using the control law

$$\Delta\underline{\theta}_{ARC}(k+1) = \underline{K} \cdot \underline{y}(k)$$

with

$\underline{K}$ feedback gain

for example [12]. With that control law the closed loop system becomes of the form shown in fig. 13 and can be described by means of the equation

$$\underline{y}(k+1) = (\underline{I} - \underline{T} \cdot \underline{K}) \cdot \underline{y}(k) + \underline{T} \cdot \underline{w}(k)$$

with

$\underline{w} = \underline{0}$ the command vector.

From this closed loop system equation it can be derived that the disturbance vector vanishes within one step if the feedback gain is set identical to the inverse of the T-matrix. Thus the theoretical possible controller response time is one step, a value which seems to be very small. The real response time required for minimization of the rotor noise and vibrations, however, can become fairly large because on the one hand the T-matrix varies with flight condition and actual point of operation. Therefore the feedback gain can not be set identical to the inverse of the T-matrix in all cases and the number of steps required for vibration and noise minimization becomes higher than one.

On the other hand, one control step corresponds to approximately two rotor revolutions in case of vibration and 10-15 rotor revolutions in case of noise reduction. The reason for the high number of rotor revolutions per control step occurring in noise case are the strong fluctuations of the Fourier coefficients from one rotor revolution to another. They need to be averaged 10-15 times before being fed back to the closed loop controller in form of the quasi-steady mean value. Thus, the time required for minimization of the noise and vibration level becomes comparatively long, a characteristic which up to now was considered to be mandatory for a frequency domain controller.

The response time is extended further in case of an online feedback gain adjustment as it was considered to be mandatory up to now at least for closed loop control of the rotor noise emissions. This is due to the fact that strong nonlinearities obviously exist in case of BVI noise and vibration reduction through active rotor control with the gradients of the intrusion indices switching sign when passing either through the global or a local extremum (fig. 14, 15).

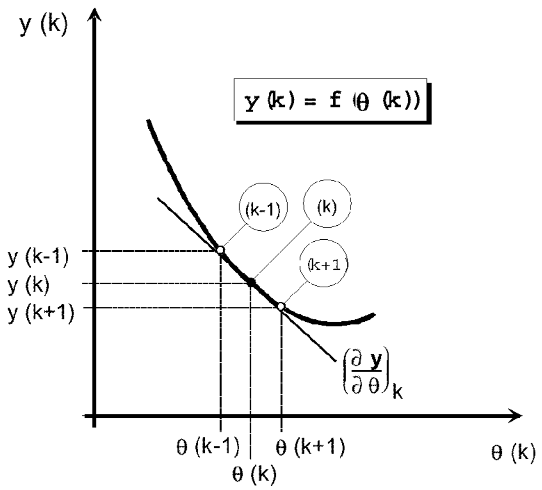

Fig. 12 Linearization Around Actual Point of Operation

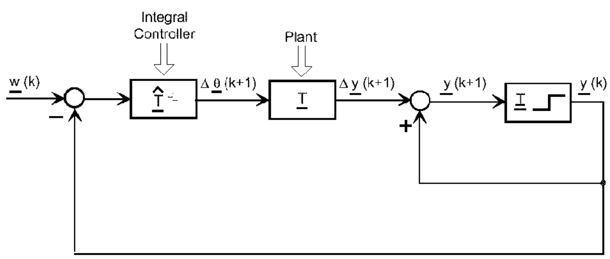

Fig. 13 Quasi-Steady Operating Closed Loop System

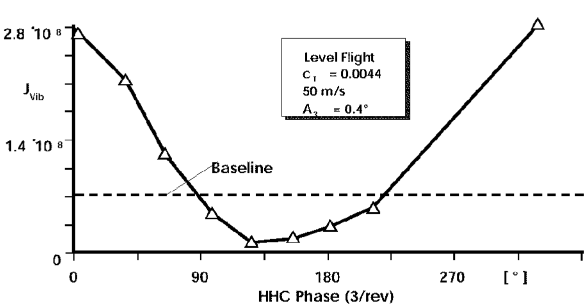

Fig. 14 Variation of Vibration Intrusion Index with 3/rev Phase Shift

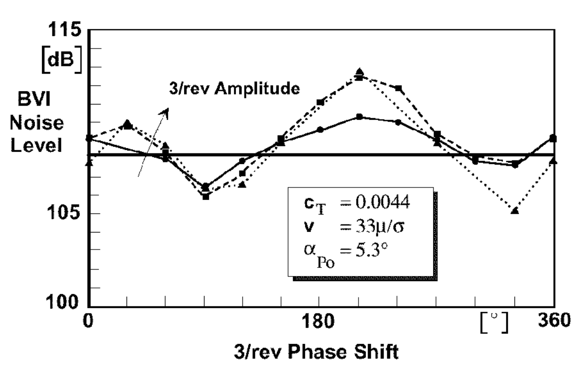

Fig. 15 Variation of BVI Noise Level with 3/rev Phase Shift

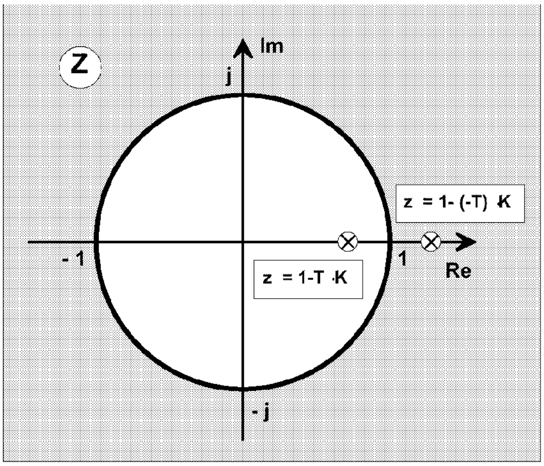

Fig. 16 Pole Placement for Conversion of Control Efficiency

Therefore a closed loop control algorithm which works with direct feedback of the intrusion indices is faced with a conversion of the control efficiency

$$T_{Vib} = \frac{\Delta J_{Vib}}{\Delta \theta_{ARC}}$$

and

$$T_{BVI} = \frac{\Delta J_{BVI}}{\Delta \theta_{ARC}}$$

respectively and thus needs to adjust at least the gain setting sign accordingly online. The necessity for this online adjustment of the gain settings when feeding back the vibration and/or BVI noise intrusion indices directly can be demonstrated easily for the single input/single output case where the characteristic system equation follows from the closed loop system equation as

$$z - 1 + T \cdot K = 0$$

and thus

$$z = 1 - T \cdot K$$

demonstrating that z becomes located outside the unit circle as soon as the sign of T and K differ from each other (fig. 16). Therefore the gain setting needs to be adapted online according to the actual value of T in order to avoid a controller instability.

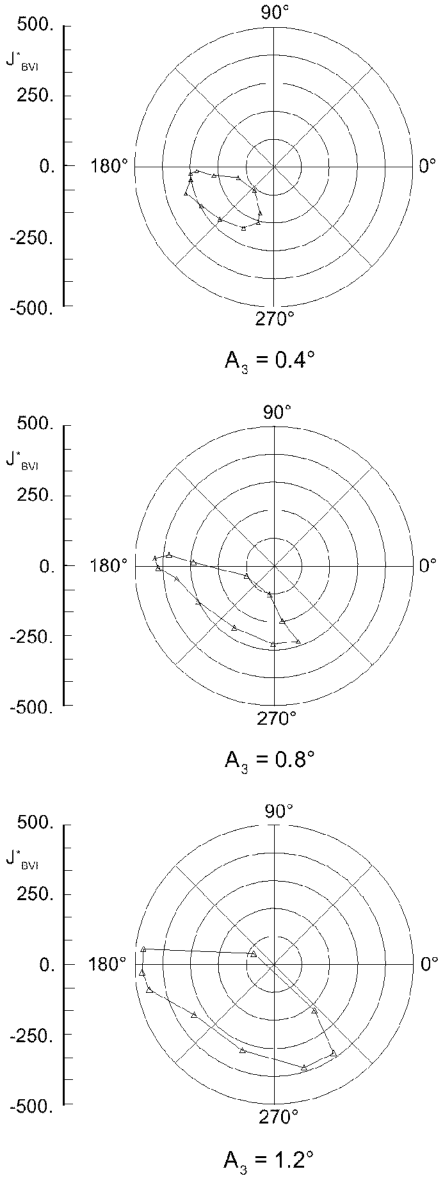

Fig. 17 ARC Effect on 38/rev Noise Emissions

The online adjustment of the gain settings, however, can be omitted when vibrations and noise are not fed back as scalar values but in form of a Fourier coefficient subset. If the real and imaginary parts of these Fourier coefficients are arranged within the vibration and BVI noise feedback vector according to

$$\underline{y}_{Vib}^{T} = \left(F_{x4R}, F_{x4I}, \cdots, M_{y4R}, M_{y4I}\right)$$

and

$$\underline{y}^{T} = \left(BVI_{38R}, BVI_{38_I}, \cdots, BVI_{46_R}, BVI_{46_I}\right)$$

respectively, the effect of active rotor control inputs to noise and vibrations can be formulated in a linear way. In order to find out to what degree this linear formulation corresponds to reality, HHC wind tunnel data were used [13]. This time the real and imaginary part of the 38/rev noise emissions were plotted against each other. The resulting vector diagrams are shown in fig. 17 which demonstrates that the 38/rev BVI noise vector describes a closed line around a point corresponding to the baseline case. Since the surrounded area increases clearly with ARC amplitude and the vector surrounds the area exactly one time when the ARC phase shift is varied from 0° to 360°, the linear formulation of the ARC effects on the rotor noise can be assumed to be valid. Due to this fact, no sign conversion of the control efficiency needs to be feared for what reason a robust closed loop control system can be designed.

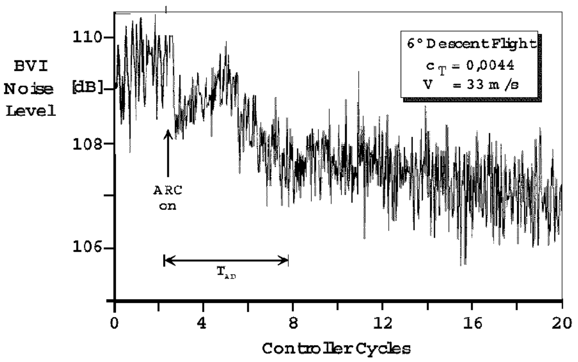

Fig. 18 Performance of Quasi-Steady Operating Controller with Online Gain Adaption

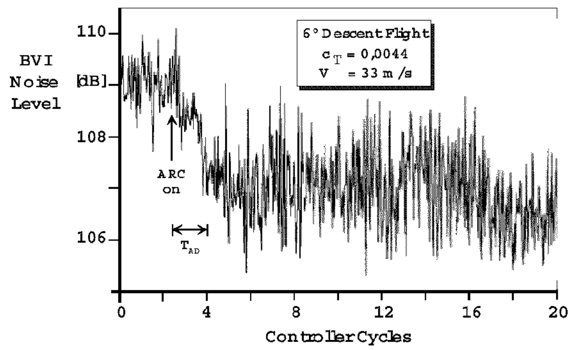

Fig. 19 Performance of Quasi-Steady Operating Controller with Fixed Gain

The advantage of that type of control system compared to an adaptive one could be determined by testing both type of controllers in combination with the DLR rotor test rig in the DNW [13]. The results are shown in fig. 18 and 19 which demonstrate that the number of steps required to reach the disturbance minimum is much lower in case of a robust controller. However, since one control step corresponds to 15 rotor revolutions, the controller response time is still too high and needs to be reduced further.

4.2 Derivation of High Order Control Laws

This required reduction of the system response time can be achieved when the design of the frequency domain controller is not based on the quasi-steady T-matrix approach but on a model which is able to describe the steady-state as well as the transients of noise and vibrations. A model of that type can be achieved by investigating the reaction of the noise and/or vibration Fourier coefficients to ARC step inputs being represented by a stepwise change of the ARC control amplitude. Fig. 20 shows for example the ARC signal of an actuator working with 4/rev and changing its amplitude of operation between revolution 6 and 7. The reaction of the 4/rev vibrations to that ARC step input is shown in fig. 21 which demonstrates that the rotor disturbances behave approximately like a system of 2nd order which is well damped and which reaches the steady state within 2 rotor revolutions. With this knowledge it is possible to design a closed loop control algorithm which allow a reduction of the rotor disturbances within very short time. In opposition to an algorithm which is based on the T-matrix model this control algorithm does not wait until the transients decay before the next cycle is initiated but which works with 64 steps per revolution.

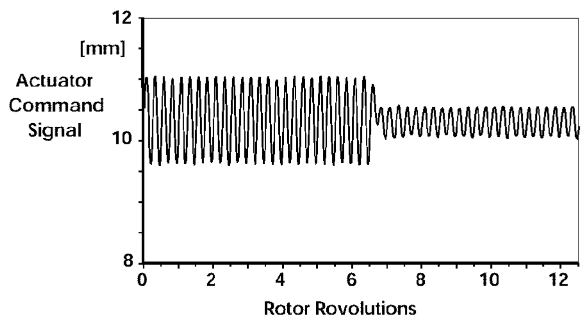

Fig. 20 ARC Step Input

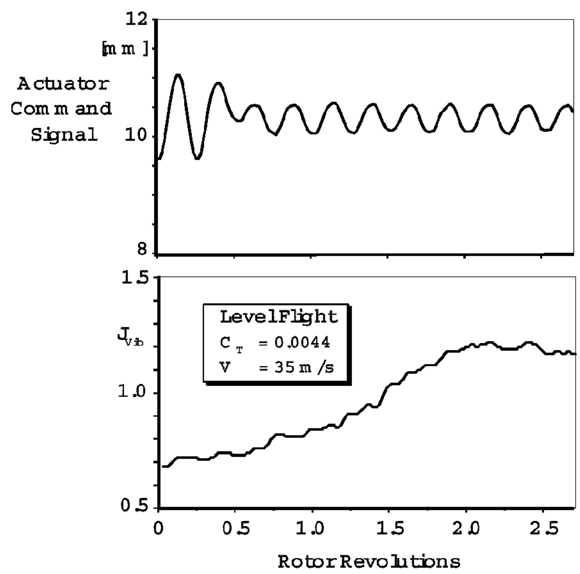

Fig. 21 Reaction of Rotor Disturbances to ARC Step Inputs

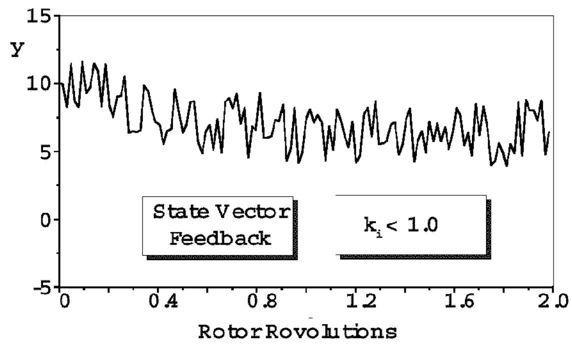

Fig. 22 Performance of High Order High Gain Controller

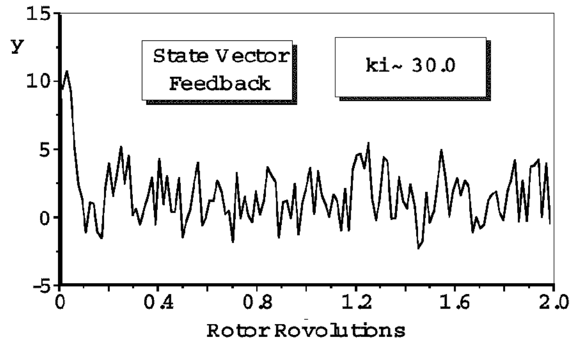

Fig. 23 Performance of High Order High Gain Controller

In fig. 22 and 23 the results of a controller are shown on which the system output vector is fed back. The results originate from numerical simulations of a 2nd order system consisting of a mass, damper and spring (fig. 24) and being excited with a force that leads to oscillations y_0 with 4/rev. The control objective is to eliminate the oscillations by determination of a suited control input amplitude at the spring. From fig. 22 it can be seen that the objective is already achieved when the feedback is selected to be comparatively low, i.e. smaller than 1. In this case the controller reaches steady state after approximately 0.8 rotor revolutions although the system output was heavily disturbed in order to account for the strong fluctuations of the noise intrusion index measured in wind tunnel. This result can be improved further when the feedback gains are increased (fig. 23) In this case steady state is already reached after 0.2 rotor revolutions and maintained although the heavily disturbed feedback signals are fed back via gain settings of approximately 30.

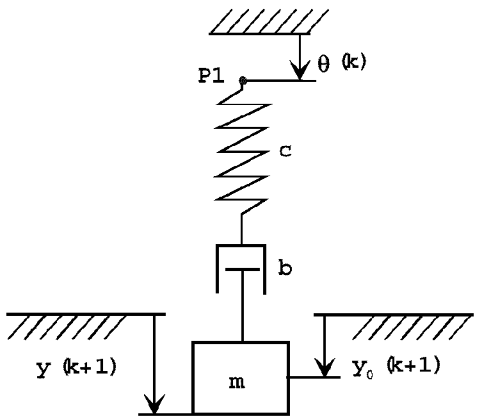

Fig. 24 System of 2nd Order for Numerical Simulations

5. Conclusions

Wind tunnel results with active rotor control demonstrated the necessity to work with high order control laws in order to reduce the rotor noise and vibrations within acceptable time. On the basis of results from step input tests a dynamic model for description of the disturbance reaction to ARC inputs was identified and two control algorithms working with output vector feedback were developed. Numerical simulations of the control algorithms in combination with the identified model showed that a stable behavior can be achieved despite of strong disturbances on the feedback signals. The controller response time is less than one rotor revolution even in case of low gain feedback.

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