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Analysis of the Behavior of a Linear Axis with an Industrial Robot

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Abstract

Industrial robots offer a variety of advantages for large-part manufacturing, e.g. the machining of vertical tail planes or wing spars, over conventional machine tools. Their compact footprint and modular design enable broad industrial applicability. However, the spherical workspace of a single articulated industrial robot may be insufficient for large parts. To overcome this limitation, industrial robots are combined with workspace extensions like linear axes. When the linear axis is interpolated with the robot joints, it enables continuous process movement, increasing production speed and flexibility. Mounting the robot on the linear axis introduces the carriage, a dynamic body with distinct properties, into the kinematic chain; consequently, as the first dynamic element, the carriage significantly influences the articulated robot and the combined system's dynamics. Experiments on a linear axis designed for path-accurate robot processes including two rack-and-pinion drives demonstrate that the coupling effects of the linear axis and the articulated robot, as well as the dynamic properties of the combined system, are pose-dependent. The optimal integration of the external axis into the control system requires a thorough characterization of these dynamic properties. This work presents a rigid multibody model inspired by an inverse pendulum that couples the linear axis translatory with the mounted robots' rotary axes movements at acceleration level and by trigonometric terms. The model reveals how dynamic properties of the linear axis change according to the robots pose and its multi-DOF stiffness and damping properties. This enables the optimization of control system parameters and the narrowing of filter bandwidths. An approach for industrial controller tuning based on the introduced model is presented. Implementation focuses on the linear axis within the industrial controller, with improvements validated through path measurement of the tool center point of the robot.

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1. Introduction

Rising global air traffic and the goal of becoming “climate-friendly” necessitates thousands of new aircraft over the coming decades. This leads to unprecedented demand for large-scale aerospace components like wings, fuselage sections, and vertical tail planes [1, 2]. Manufacturing these structures poses challenges: long machining cycles, low production volumes, and frequent changeovers significantly impact profitability. At the same time, the industry's shift toward modular, energy-

efficient aircrafts requires flexible production systems that can be adapted quickly and cost-effectively [3]. Industrial robots (IRs) mounted on linear axes meet these requirements. Their kinematic flexibility enables precise, scalable machining of large aerospace components while reducing setup times and operating costs [4]. In addition to the classic tasks of handling and assembly technology, they are now established in areas of application such as painting, laser welding, and machining [5, 6]. High versatility combined with energy efficiency and low acquisition costs are attractive properties not only for large

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industrial facilities but also for medium-sized enterprises. Especially in the field of machining, IRs show the potential to be the foundation for variable manufacturing applications [6].

Comprehensive processing requires machining systems to cover the entire workpiece dimensions. To address the challenges of long cycle times and high investment costs, research is focused on smaller, mobile machines. Often it is sufficient to extend the workspace of these smaller machines in one degree of freedom, e.g. an external linear axis (LA) significantly expands the workspace of IRs [4, 7]. In addition, multiple IRs mounted on a single LA can operate simultaneously, resulting in shorter processing times and increased flexibility. If the LA is considered directly as part of the robot, the additional axis could also replace the robot's first axis, resulting in increased rigidity [8].

The main challenge for an external axis with long travel ranges is the demanded accuracy without constraining the productivity of the mounted machining system. Rack-and-pinion drives (RPD) are used for longer travel distances, as the resulting overall rigidity of a RPD is always independent of the length of the travel distance [9, 10]. In addition, the racks offer a modular design and thus enable very long travel distances. The elimination of reverse effects and clearance of the gears is critical for precise motion transmission and thus for applications requiring high path accuracy. There are two ways to eliminate clearance: mechanically, using spring-loaded pinions, or electrically, by incorporating a second motor [9, 11]. The advantage of electrical preload is that it can be optimally adjusted in terms of backlash, friction, static stiffness, and bandwidth [11]. In addition, new approaches offer the possibility of adapting the preload depending on the state of the total reference torque or position error [12, 13].

Investigations into the dynamic behavior of the robot reveal a strong dependence on the robot's pose [14–16]. As a first dynamic body in a series of many, the LA has a significant influence on the mounted articulated robot and on its dynamic behavior. As mentioned at the beginning, the dynamic influence of the robot on the LA changes with the robot's pose. To suppress unwanted dynamic influences a well-tuned control system is essential, for which a deep understanding of the dynamic system in its entirety is necessary.

1.1. Scope of this work

This paper aims at understanding and modelling the dynamic behaviour of a LA with a mounted articulated robot and addresses the scientific question: Which model sufficiently describes the dynamics of a driven LA with a mounted articulated robot to optimize the control for a LA-robot system?

The focus is on different control side measurements to illustrate the dynamic behaviour of LA with mounted IR. This involves investigating the extent to which the pose-dependent behavior of the robot influences the dynamics of the LA and its drives. Based on these findings, a model approach for describing the dominant dynamic properties of the LA with the articulated IR inspired by an inverted pendulum are outlined. Altering the position of the robot, and thus the static equilibrium of the model, results in the excitation of different axes as well as different directions from the robot's perspective.

In addition, the dynamic properties of the LA drive system with gearbox based on a simple two-mass spring system are considered. The resulting rigid multibody model effectively captures the dominant dynamic characteristics of a LA with a mounted IR. Finally, an approach to optimize the industrial control with the insights gained by the multibody dynamic model of the system is presented.

Nomenclature

IR	Industrial Robot
LA	Linear Axis
RPD	Rack-and-Pinion Drives
A1, A2, A3	Axis 1, Axis 2, Axis 3
TCP	Tool Center Point
P	Proportional
I	Integral
FRF	Frequency Response Function
DoF	Degree of Freedom
CoM	Center of Mass

2. Experimental Setup

This section presents the experimental setup shown in Fig. 1, on which the subsequent experiments and modelling approaches are based. It consists of a LA with mounted robot. The overall length of the LA is seven meters, whereby carriage width and safety features such as hardware stops on both sides reduce the effective travel range to five-and-a half meter. Moreover, the feed concept consists of two RPDs, profile roller rail guides from Bosch Rexroth, and an absolute linear scale from FAGOR. Load-side measurement with linear scales compensates for position-dependent errors in mechanical transmission, e.g., thermal spindle expansion, enabling higher accuracy compared to motor-side rotary encoders [17].

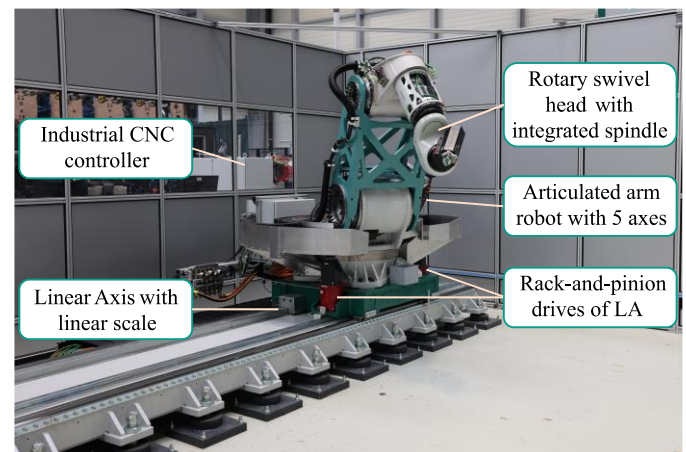


Fig. 1. Self-built serial kinematics consisting of a linear axis with an articulated arm robot for machining large parts from the aviation industry.

The mounted robot is a self-built serial kinematic designed for machining application. It features five axes with a rotary swivel head for the fourth and fifth axes. In addition, all axes have secondary encoders [18]. The lower joint axes are driven in gear mode to investigate the interaction of the low natural

frequencies, which in conventional industrial robots are attributable to the gear drives of the lower axes [8, 14]. Since electrical motors are limited by their output torque, the use of gearboxes is essential. The kinematic design of articulated robots in combination with torsional compliance of the gearboxes causes a lower static and dynamic stiffness compared to machine tools.

The system is controlled by an industrial controller SINUMERIK 840D sl, which is an advanced CNC control system designed for complex machining tasks in industrial applications. The controller implements a three-stage cascaded control structure for the drive systems [19]. Figure 2 shows the cascaded controller structure for the two mechanically coupled drives of the LA without the innermost current controller. To simplify and due to its significantly higher dynamics, the current controller is not considered in this work. The outermost position controller functions as a proportional (P)-controller that generates the velocity setpoint from position deviations. Subsequently, the velocity setpoint filter provides the option to smooth setpoint changes and limit the tracking behavior. The velocity controller operates as a PI-controller with additional integral (I) part from velocity deviation to torque setpoint. The torque setpoint filter removes critical resonance frequencies to avoid excitation of mechanical eigenfrequencies.

A torque balance controller regulates the torque distribution by setting a balancing velocity with opposite signs into both drive paths [20]. The balance controller is implemented as a proportional controller, with its gain parameter scaled in relation to the proportional term of the velocity controller [21]. This approach ensures stability since an excessive gain factor leads to instability of the balance control loop [22]. As the velocity controller's inherent integral action already effectively compensates for steady-state deviations, the integral component is omitted [22]. The input torque to the balance controller from the two motors is scaled by factors K_1 and K_2 , respectively. Since motor 1 and motor 2 are identical, K_1 and K_2 are selected as 0.5. In addition, the balance controller allows for an electrical preload torque τ_p to be applied. The preload τ_p equal to 12% of the LA motor's nominal torque is set. This ensures that both motors behave in a similar manner and that nonlinear effects, for example backlash, are avoided.

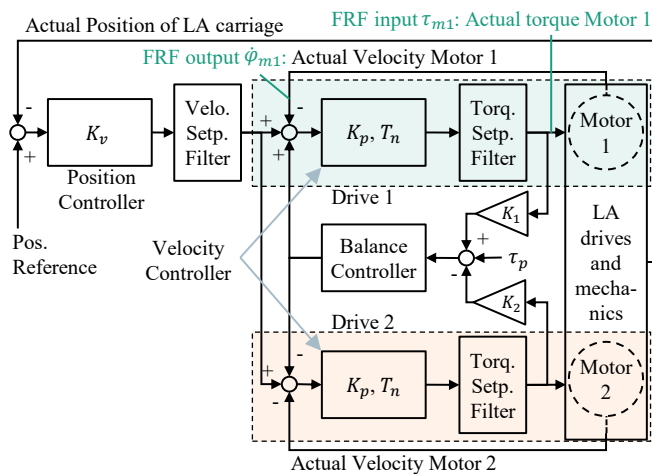


Fig. 2. Cascaded control structure of position and velocity for the two mechanically coupled motors of the linear axis with balance controller; for simplicity, the current controller is omitted

2.1. Dynamic characteristic of robot on linear axis

To obtain an initial understanding of the dynamic system at hand, the frequency response function (FRF) of the mechanical velocity-control loop is measured. This analysis provides insight into the change in magnitude and phase of the motor shaft's angular velocity $\dot{\phi}_{m1}(t)$ with respect to the torque $\tau_{m1}(t)$ applied by the LA's motor 1 at a given frequency. Here, $\dot{\phi}_{m1}$ is determined by the motor-side encoder and τ_{m1} by motor's current. The FRF is mathematically described by the transfer function $G_{m1}(s)$ in the frequency domain as

$$G_{m1}(s) = \frac{\dot{\phi}_{m1}(s) \cdot s}{T_{m1}(s)} \text{ with } \quad (1)$$

$$\dot{\phi}_{m1}(s) = \mathcal{L}\{\dot{\phi}_{m1}(t)\}, \text{ and } T_{m1}(s) = \mathcal{L}\{\tau_{m1}(t)\}.$$

To highlight the different dynamic behavior across the IR's workspace, the measurements are carried out in poses of interest, where the exposure to an excitation of the LA becomes either minimal or maximal. Figure 3 shows the FRF of LA's motor 1 in four different poses. See Fig. 6 for the mechanical context of these poses. Pose 1 represents the greatest distance from the center of mass (CoM) to LA in the x -direction, leading to a maximum excitation of Axis 1 (A1). Pose 2 and Pose 4 represent the greatest distance in z -direction. In Pose 2, the rotational axes of A2 and A3 are parallel to the LA, whereas in Pose 4 they are perpendicular and therefore exposed to excitation by the LA. Lastly, Pose 3 represents the smallest distance between CoM and LA.

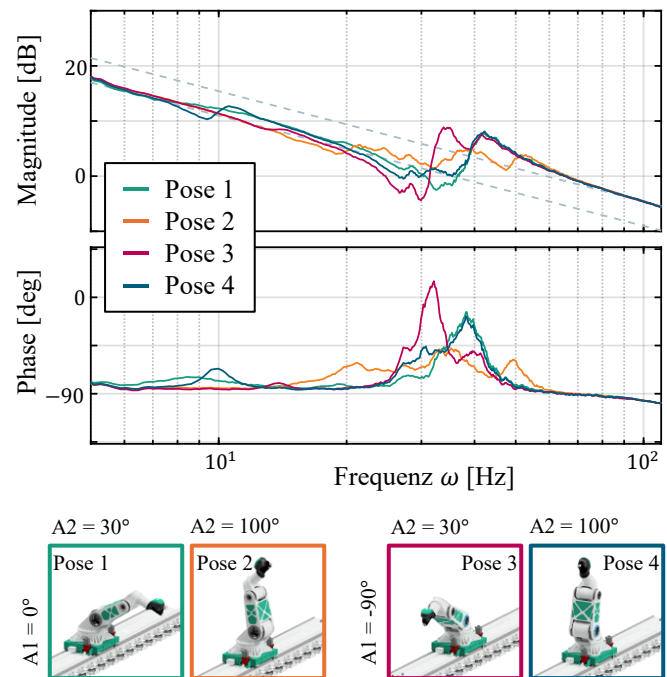


Fig. 3. Measured frequency responses of the mechanical velocity-control loop of LA's motor 1 at different robot poses; input and output are shown in Fig.2.

The most pronounced resonance and antiresonance frequency pair per graph is assigned to the drive of the LA; they are between 30 and 45 Hz. It is clearly recognizable that the dynamic properties change with the robot's pose. At Pose 4, a resonance and antiresonance are visible around 10 Hz, which is in the proximity of the eigenfrequency of IR's A2. While the

excitation near the eigenfrequency of A1 in Pose 1 is less dominant in the measurements. A slight peak in the phase shift can still be observed, indicating a high damping constant or friction force.

3. Modeling approach

This section describes the approach of modelling an articulated IR mounted on a LA. First, the drive train of the LA is described. Next, a simplified approach to describe the pose-dependent behavior of the robot on the LA carriage is proposed. Finally, the overall multibody model of LA and IR is presented.

The LA consists of two identical drive units connected to the load via gearboxes. For simplicity, the drivetrain is considered as a single unit. To model inherent gearbox flexibility, it is presented as an ideal transmission with transmission ratio i followed by a torsional spring k_g . The rotary motion φ_m and the applied LA's motor torque τ_m is converted to linear motion through a pinion-rack mechanism and is mathematically described by the pinion's radius r_p . In addition, all motor dynamics are referred to the load side for a unified multibody system analysis. Therefore, the equivalent motor force F_m , mass m_m , stiffness coefficient k_m and position y_m are given by

$$F_m(t) = \tau_m(t) \cdot \left(\frac{i}{r_p}\right), y_m(t) = \varphi_m(t) \cdot \left(\frac{r_p}{i}\right), \quad (2)$$

$$m_m = I_m \cdot \left(\frac{i}{r_p}\right)^2 \text{ and } k_m = k_g \cdot \left(\frac{1}{r_p^2}\right).$$

The resulting equations of motions describing the motor shaft combined with the LA carriage without a robot are comparable to those of a two-mass system.

$$F_m(t) = m_m \cdot \ddot{y}_m(t) + k_m \cdot (y_m(t) - y_{LA}(t)), \text{ and} \quad (3)$$

$$0 = m_{LA} \cdot \ddot{y}_{LA}(t) - k_m \cdot (y_m(t) - y_{LA}(t)) \quad (4)$$

with m_{LA} the mass of the LA and y_{LA} it's position.

3.1. Multibody system approach of the robot

A first modelling approach for an articulated robot mounted on a linear axis used in this paper is similar to an inverse pendulum. Therefore, the LA-IR system presented in Fig. 1 is simplified to a two-body system. The linear carriage is represented by a body, which can only move along the y -axis. The entire robot is represented by a second rigid body with one DoF, that tilts around a pivot point where the two bodies are connected. Alternatively, to an equivalent translatory spring-mass system, this approach connects the two bodies directly on the acceleration level. To still allow for oscillations, a torsional spring with stiffness k_{IR} connects the two bodies at the pivot point. Additionally, the robots motor torque τ_{IR} is placed at the same location, thereby establishing a static equilibrium at a specified angle φ_0 . Therefore, it's objective is to cancel out any gravitational influences in static cases. In this case, the mathematical formula to describe τ_{IR} is given by

$$\tau_{IR} = g \cdot m_{IR} \cdot z_{IR} \cdot \sin(\varphi_0). \quad (5)$$

This approach captures the pose-dependent dynamic coupling between the LA carriage with mass m_{LA} and robot body described by mass m_{IR} and inertia I_{IR} . The system's degrees of freedom are the carriage position $y_{LA}(t)$ and robot angle $\varphi_{IR}(t)$, which represents the rotation of the robot body. The LA carriage is excited by the force F_{LA} . In the overall model of section 3.2, F_{LA} equals F_m and refers to the motor torque τ_m of the LA described in equation (2). Fig. 4. depicts the simplified two-body model of LA carriage and IR body.

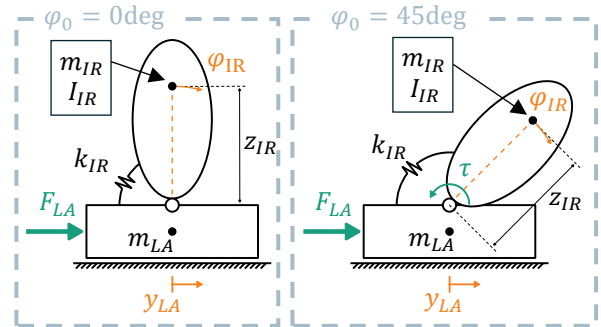


Fig. 4. Sketch of an articulated robot modelled as an inverse pendulum

The equations of motion derived from this model are given by the general expression in minimal form

$$\mathbf{M}\ddot{\mathbf{u}} + \mathbf{k} = \mathbf{q}, \text{ with } \mathbf{u} = [y_{LA} \quad \varphi_{IR}] \quad (6)$$

and the generalized mass matrix $\mathbf{M}$. The vector $\mathbf{k}$ describes Coriolis, centrifugal, and gyroscopic forces, and the forces and moments exerted are expressed as $\mathbf{q}$. A description of the derivation of the mass matrix, the vectors of the velocity-dependent inertia terms, and the generalized driving forces can be found in [23]. For holonomic multibody systems, $\mathbf{M}$ is defined by

$$\mathbf{M}(\mathbf{u}, t) = \sum_{i=1}^n (\mathbf{J}_{T,i}^T m_i \mathbf{J}_{T,i} + \mathbf{J}_{R,i}^T \hat{\mathbf{I}}_i \mathbf{J}_{R,i}), \quad (7)$$

the vector of generalized Coriolis, centrifugal and gyroscopic forces $\mathbf{k}$ by

$$\mathbf{k}(\mathbf{u}, \dot{\mathbf{u}}, t) = \sum_{i=1}^n (\mathbf{J}_{T,i}^T \bar{\mathbf{a}}_i + \mathbf{J}_{R,i}^T \hat{\mathbf{I}}_i \bar{\mathbf{a}}_i + \mathbf{J}_{R,i}^T \tilde{\boldsymbol{\omega}}_i \hat{\mathbf{I}}_i \boldsymbol{\omega}_i), \quad (8)$$

and the generalized applied forces by

$$\mathbf{q}(\mathbf{u}, \dot{\mathbf{u}}, t) = \sum_{i=1}^n (\mathbf{J}_{T,i}^T \mathbf{f}_i^a + \mathbf{J}_{R,i}^T \mathbf{l}_i^a). \quad (9)$$

In the equations (7) to (9), the Jacobian matrix of translation and rotation of body i are represented by $\mathbf{J}_{T,i}$ and $\mathbf{J}_{R,i}$, $\hat{\mathbf{I}}_i$ denotes the transformed inertia tensor, $\bar{\mathbf{a}}$ and $\bar{\mathbf{a}}$ corresponds to the linear and angular acceleration, $\boldsymbol{\omega}$ refers to angular velocity vector, $\tilde{\boldsymbol{\omega}}$ indicates the skew-symmetric angular velocity matrix, and $\mathbf{f}_i^a$ respectively $\mathbf{l}_i^a$ the applied forces and torques. For scleronomic systems the explicit time dependency vanishes [23].

For the system presented in Fig. 4, equations (7), (8), and (9) result in

$$\mathbf{M} = \begin{bmatrix} m_{LA} + m_{IR} & -m_{IR}z_{IR} \cos(\varphi_{IR}) \\ -m_{IR}z_{IR} \cos(\varphi_{IR}) & m_{IR}(z_{IR})^2 + I_{IR} \end{bmatrix}, \quad (10)$$

$$\mathbf{k} = \begin{bmatrix} m_{IR}z_{IR}(\dot{\varphi}_{IR})^2 \sin(\varphi_{IR}) \\ 0 \end{bmatrix}, \text{ and} \quad (11)$$

$$\mathbf{q} = \begin{bmatrix} F_{LA} \\ k_{IR}(\varphi_0 - \varphi_{IR}) - \tau_{IR} + g m_{IR} z_{IR} \sin(\varphi_{IR}) \end{bmatrix}. \quad (12)$$

As these equations contain non-linear terms, they are not directly suitable for control system design and therefore have to be linearized. The linearisation used in this approach is described in [23]. It is only valid if the robot's angle φ_{IR} stays in the vicinity of a static equilibrium defined by $\mathbf{u}_s = [y_0, \varphi_0]^T$. The equations of motion of the linearized system are given by the general expression

$$\mathbf{M}_0 \ddot{\mathbf{u}}(t) + \mathbf{D} \dot{\mathbf{u}}(t) + \mathbf{K} \mathbf{u}(t) = \mathbf{h}(t) \quad (13)$$

with linearized mass matrix $\mathbf{M}$, damping matrix $\mathbf{D}$, stiffness matrix $\mathbf{K}$ and force vector $\mathbf{h}$. As the example system presented in this section does not contain any dampening elements, $\mathbf{D}$ equals a zero matrix. The other matrices are given by

$$\mathbf{M}_0 = \begin{bmatrix} m_{LA} + m_{IR} & -m_{IR}z_{IR} \cos(\varphi_0) \\ -m_{IR}z_{IR} \cos(\varphi_0) & m_{IR}(z_{IR})^2 + I_{IR} \end{bmatrix}, \quad (14)$$

$$\mathbf{K} = \begin{bmatrix} 0 & 0 \\ 0 & k_{IR} - g m_{IR} z_{IR} \cos(\varphi_0) \end{bmatrix}, \text{ and} \quad (15)$$

$$\mathbf{h} = \begin{bmatrix} F_{LA} \\ 0 \end{bmatrix}. \quad (16)$$

The equations (14) to (15) reveal that dynamic coupling between translational and rotational motion depends directly on the robot's pose through the term $\cos(\varphi_0)$. This mimics the pose-dependant behaviour observed in the experiments. Fig. 5 shows simulation results of the transfer function from the force excitation to the position of the LA for varying equilibria.

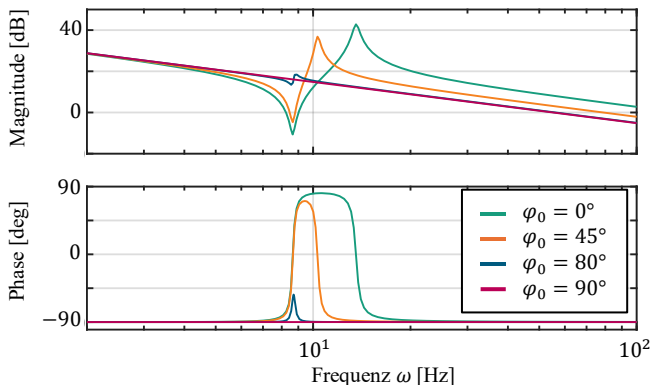


Fig. 5. Frequency responses of the mechanical velocity loop of the carriage of the model shown in Section 3.1 at different robot positions

The resonance, which is coupled in by the rotational movement of the robot, disappears the closer the equilibrium point under consideration comes to 90°. When the robot is horizontal ($\varphi_0 = 90^\circ$), the off-diagonal terms of equation (14) vanish, decoupling the rotational movement of the robot from the carriage excitation. At 90°, only a translational movement of the mass block is performed.

3.2. Multibody Model of Linear axis and robot

In a second step, the multibody model of an articulated robot mounted on a linear axis combines the drivetrain of the LA and an extended version of the robot's multibody system introduced in section 3.1. To extend the model of the robot, it is divided into multiple bodies, instead of just one. The influence of the hand axes is considered minor and is therefore neglected. The states of this model are the position of the LA motor and carriage as well as the angles of the first three robot axes. In addition, φ_x describes a tilting movement around the x' -axis, which is a result of the bearing stiffness of axis 1 or the structure. This is visible in the examined Pose 2, compare Fig. 3. The variables of the degrees of freedom of the overall model are summarized as follows

$$\mathbf{u} = [\varphi_m \quad y_{LA} \quad \varphi_x \quad \varphi_{A1} \quad \varphi_{A2} \quad \varphi_{A3}] \quad (17)$$

Figure 6 shows the individual bodies of the multibody system; the system comprises five bodies and six DoF. Thereby, joint 1 has two DoF's.

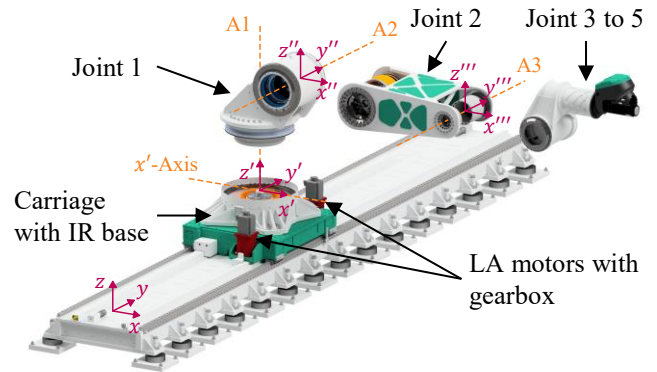


Fig. 6. Rigid multibody system of the system LA with mounted IR considering the first three robot axes

Table 1 presents an overview of all parameter values of the multibody model depicted in Fig. 6. The mass and inertia of the robot's bodies are given in their respective body fixed coordinate systems and estimated by a CAD-program. This estimation is assumed sufficiently accurate for the purpose of this model and understanding the model dynamics. Furthermore, transmission ratios, pinion radius, motor inertia, damping coefficients and spring stiffnesses are derived from data sheet values with manual fitting. The stiffness around the x' -axis is solely a mechanical stiffness; the stiffnesses of the robot axes are the adjustments resulting from the control around the equilibrium positions.

Table 1. Overview of the model parameter.

Param.	Value	Unit	Param.	Value	Unit
k_m	$1.7 \cdot 10^8$	N/m	d_m	$1.3 \cdot 10^5$	N/m
k_x	$1.3 \cdot 10^8$	Nm/rad	d_x	$3.0 \cdot 10^4$	Nms/rad
k_{A1}	$6.7 \cdot 10^6$	Nm/rad	d_{A1}	$3.3 \cdot 10^4$	Nms/rad
k_{A2}	$6.6 \cdot 10^6$	Nm/rad	d_{A2}	$6.0 \cdot 10^3$	Nms/rad
k_{A3}	$1.9 \cdot 10^6$	Nm/rad	d_{A3}	$3.0 \cdot 10^3$	Nms/rad
m_m	3717	kg	m_{LA}	1800	kg
m_{I1}	680	kg	m_{I2}	590	kg
m_{I35}	250	kg	i	50	-
r_p	0.0425	m	$\tau_{m,nom}$	11.2	Nm
Rotational Body	$\hat{I}_{xx}$	$\hat{I}_{yy}$	$\hat{I}_{zz}$	Unit	
Body A1	102	117	108	kg·m ²	
Body A2	54	127	161	kg·m ²	
Body A3	29	78	61	kg·m ²	

The input of the system corresponds to the torque $\tau_m(t)$ exerted by a motor of the linear axis. The behavior of the model compared to the experimentally determined values is again considered using the transfer function of the velocity control loop according to Eq. (1). Figure 7 shows the transfer functions for the model and the experiment, in different poses.

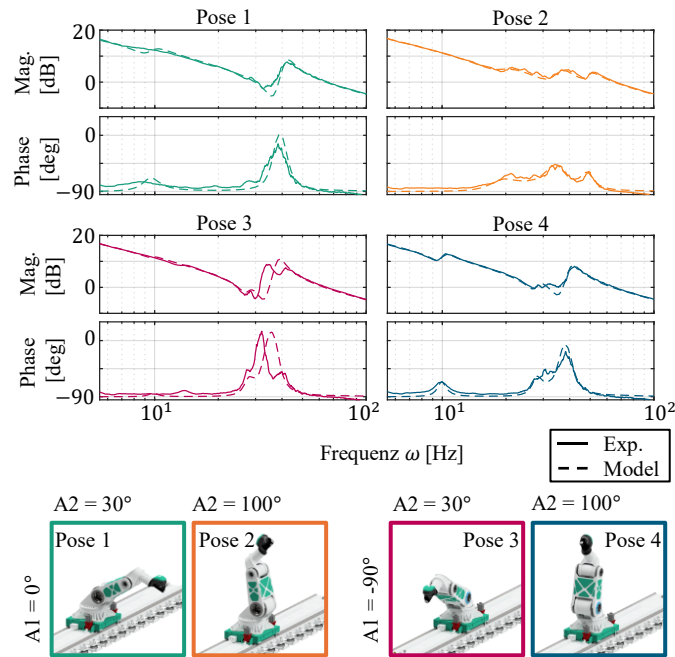


Fig. 7. Comparison of measured frequency responses of the mechanical velocity-control loop at different robot poses with the results of the rigid multibody model.

The damping and resonance frequency of the linear axis motor range between 30 and 50 Hz at the maximum phase in all poses. Its location is only slightly influenced by the robot pose. The other frequencies are excited depending on the pose; the model also exhibits this behavior. In Pose 1 and Pose 4, the rotational stiffness of A1 and A2 is most strongly excited in the range of 10 Hz. The model reflects this behavior, while in the measurements only Pose 4 leads to low-frequency excitation. Similar to the excitation of A2, the influence of A3 occurs in

Pose 3 and Pose 4 and is to be allocated at 30 Hz, which is close to the drive frequency of the LA. The tilting stiffness around the x' -axis is significantly higher than the rotational stiffness of A1 to A3, resulting in minor excitation when the movement of the LA carriage excites one of the lower robot axes. In Pose 2, however, none of the lower axes are excited, but there is response in the range of 45 to 55 Hz, which, according to the model, indicates the modeled tilting stiffness. Discrepancies between the model and measurements may indicate unmodeled influences, e.g. friction.

4. Control

The model presented provides further insights into the behavior of the robot regarding LA dynamics. On this basis, the parameterization of the controller depicted in Fig. 2 of the LA gear drives is carried out. Approaches in literature focus to ensure high bandwidth and minimal tracking errors while avoiding resonance excitation introduced by adhering mechanical components [24]. For the LA system, this primarily means parameterizing the velocity control loop in a way that optimizes damping, allowing high positioning gains and therefore bandwidth. Consequently, this leads to oscillation-free positioning of the carriage. Linear encoders on the carriage feed actual position to the control loop, detecting transmission- and backlash-induced deviations.

In interaction with the IR, the optimal behavior of the tool center point (TCP) is of particular interest. Its state variable is not present in the LA control loop. However, the behavior of the TCP can be predicted based on the introduced model.

This chapter centers on two distinct approaches to derive two sets of control design parameters. The first features an optimal tracking controller based on a simplified two mass model of the LA drive train [24], whereas the second is a disturbance oriented controller which takes advantage of the multibody model from Chapter 3.2 [25]. Lastly, both controllers are compared in a series of experiments.

4.1. Controller Design

The optimal tracking controller design is based on the simplified two mass model depicted in equation (3) and (4). Consequently, it features two masses: one for the motor shaft and one for the load, which in this case is the LA carriage as well as the IR. Following equation (2), their masses are converted to the drive side inertias J_m for the motor and J_{load} . Both values can be read from the transfer function in Fig 3: J_m equals a simple integrator line for $\omega \rightarrow \infty$, J_{load} equals a simple integrator line for $\omega \rightarrow 0$. Lastly, the resonance frequency associated with the LA $\omega_{0,LA}$ or the respective antiresonance frequency $\omega_{n,LA}$ is needed and is also determined by the FRF plot. With these values, the factor κ_{opt} is described according to [24] or [25] by

$$\kappa_{opt} = \omega_0 \cdot \left(\frac{J_m}{J_m + J_{load}} \right)^{\frac{1}{\sqrt{2}}} \text{ or } \kappa_{opt} = \omega_n \frac{1}{\sqrt{2}}. \quad (18)$$

Here, κ_{opt} denotes the optimal velocity controller gain that maximizes damping ratio for elastically coupled systems [24].

Subsequently, based on κ_{opt} the values of the velocity controller are calculated by

$$K_p = \kappa_{opt} \cdot (J_m + J_{load}) \quad (19)$$

for the P gain K_p scaled with the total inertia to maintain torque responsiveness and the integral time T_N with

$$T_N = 2/\kappa_{opt} \quad (20)$$

to ensure a damping ratio equal to $1/\sqrt{2}$ [25]. Finally, the gain factor of the position controller is then determined by assuring minimal oscillation with at least 70° Phase or 12 dB Gain-margin [24, 25]. Although the tracking optimal controller shows desirable positioning behavior of the LA, the parallel positioning of the robot TCP is associated with strong overshoot and oscillations, as shown in Fig. 8.

The second controller design adapts a damping-oriented approach for higher disturbance rejection, while also considering the multibody dynamics of the robot. The damping-oriented approach is primarily achieved by increasing the velocity gain and decreasing the integral time. Consequently, the controller is able to react faster to deviations and offers an increased bandwidth. While this is a desirable outcome, it also strongly excites the lower resonance frequencies of the robot, which can cause slowly fading oscillation. Simulations with the multibody model from Chapter 3.2 uncover a minimal damped oscillatory behavior of the appended masses and stiffness couplings in Pose 1 and Pose 4. These are caused by the lower resonance frequencies in the range of 10 Hz, as they are inside the bandwidth of the closed control loop. There are two adjustments possible to cope with the low resonance excitation. With the procedure described in equation (18) to (20), the controller design could be performed for the lower frequencies of the robot $\omega_{0,IR}$ or $\omega_{n,IR}$ around 10 Hz. This results in a significantly smaller velocity proportional gain and thus considerably reduces disturbance rejection. Another option is the use of a setpoint filter, positioned at the lower resonance frequencies, which also reduces the tracking performance while keeping the same disturbance rejection ability. This approach will be pursued further, and a setpoint filter is implemented as a notch filter with attenuation after the resonance frequency. It is positioned at the first resonance frequency of the robot $\omega_{0,IR}$. In addition, the position gain is decreased assuring the mentioned margins. The resulting loss in tracking performance is acceptable since the overall performance of the system is limited by the weakest axis, which is in the IR.

Both controller designs are listed in the table 2. Set 1 describes the tracking-optimal controller for the LA drive, and Set 2 describes the disturbance-optimal controller, considering the multibody dynamics of the system LA with mounted IR.

Table 2. Overview of control parameter Set 1 tracking controller and Set 2 disturbance controller.

Controller Set	K_v [$m/min/mm$]	K_p [Nms/rad]	T_n [ms]	Setp. Filter
Set 1 (Track.)	1.50	0.6	17	No
Set 2 (Dist.)	0.75	2.4	8	Yes

4.2. Experimental Validation

The results of the experimental investigations of the two controllers from table 2 are presented. Two aspects are highlighted, the tracking performance of the LA carriage and TCP; as well as the disturbance rejection capability. For the first assessment a path planned step is applied to the LA controller while measuring the LA carriage's and robot axis' secondary encoders, which are internally calculated to the TCP actual position. Figure 8 shows the result for the position of the LA carriage and the simultaneous TCP movement.

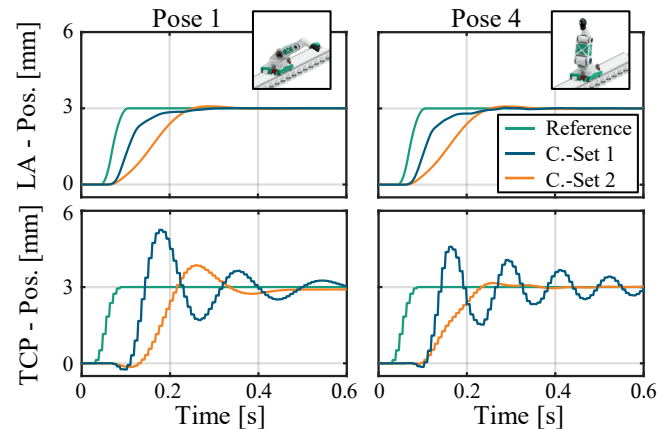


Fig. 8. Experimentally determined position change of a 3 mm step of the LA carriage (top) and the corresponding TCP position of the robot (bottom)

The step responses of the LA show, an almost oscillation-free approach to the target position. The higher K_v factor of the tracking-oriented controller (Set 1) results in a shorter rise time. The excitation through the LA induces oscillations in the appended masses, which for the tracking-optimal controller are clearly visible as a sustained oscillation of the IR's TCP. In contrast, the disturbance-oriented controller (Set 2), equipped with a velocity setpoint filter at the frequency of the excited oscillation, can significantly reduce this excitation.

To illustrate the disturbance rejection properties, Figure 9 shows the recorded encoder signals of the LA while a step input was applied to A2 of the IR in Pose 4. The second control approach shows superior suppression behavior for disturbances acting on the motor shaft of the LA. Summarized, the disturbance orientated approach with the usage of set point filter offers a good set point guidance without excessively exciting resonances visible at the TCP. The disturbance rejection capability is limited to the LA and its drive.

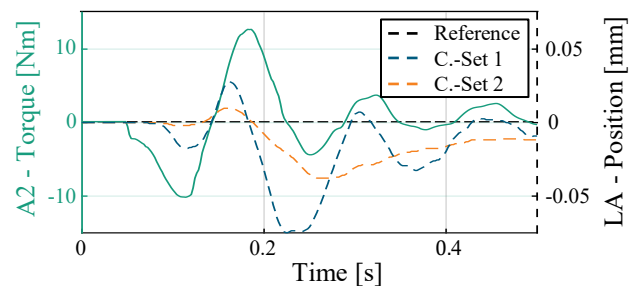


Fig. 9. Influence of disturbance caused by movement of robot axis 2 in a perpendicular orientation (Pose 4) on the position of the linear axis carriage

5. Conclusion

In this work, a multibody system model of a LA with a mounted IR is presented, which describes the pose-dependent behavior of the robot visible in the dynamic behavior of the LA drive. The model enables detailed conclusions to be drawn about the dynamic interactions and contributes significantly to the understanding of the system dynamics. On this basis, a cascaded controller structure was designed analogous to the established procedure for two-mass spring system describing the conventional gear drive of the LA.

Although the controller exhibits good tracking behavior of the LA carriage, the higher dynamics of the external axis compared to the IR leads to undesirable excitation of the mounted robot, which significantly increases the TCP position error. The model-based understanding of the system enabled targeted optimization of the controller using setpoint filters, which significantly improved the dynamic behavior at the TCP and effectively suppressed vibration excitation.

In the future, the developed model will offer the possibility to design advanced controllers and perform model-based feedforward control. In this context, the parameterization of the advanced position control function of the SINUMERIK controller is of immediate interest, as this state-space controller is able to map further system dynamics compared to the cascaded structure [19, 21]. As far as the model predictive control algorithm is concerned, CNC systems are often closed, so implementations focus on additional high-level process control [26], e.g., synchronizing interactions between LA and IR to optimize coordinated actions. If linearization of the model is required for feedforward or feedback control, note that the linearization of Eq. 13 is independent of the position of the LA, but only applies to IR configurations that are close to the linearization pose. Therefore, precalculated lookup tables of pose-dependent model matrices (M , D , K) or dynamic re-linearization with greater computational effort are required for industrial applicability. In addition, integrating LA with IR dynamics into offline path planning simulations enables motion adaptations for improved path accuracy.

CRedit authorship contribution statement

Stephan Hansen Writing – review & editing, original draft, Visualization, Conceptualization, Methodology, Investigation. **Nikolas Winter** Writing – original draft Section Modeling, Conceptualization, Investigation. **Tobias Hamann** Writing – original draft Section Control, Controller synthesis, Investigation. **Robert Seifried** Writing – review & editing. **Wolfgang Hintze** Writing – review & editing.

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